Experimental and Numerical Investigations of the Behaviour of Intact Veined Andesite

by

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A thesis submitted in conformity with the requirements for the degree of Doctor of Philosophy
Department of Civil Engineering
University of Toronto

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Abstract

Veined rock masses host some of the world’s largest mineral deposits. The fundamental behaviour of veined rock mass under load has not been fully understood. Currently there is an absence of validated engineering tools to undertake a comprehensive analysis of the behaviour of veined rock. The main objectives of this thesis are (1) to characterize the mechanical behaviour of intact veined rock in the laboratory environment in compression and (2) to develop and validate an engineering approach that captures the behaviour of intact vein rock using 3D numerical simulations.

A comprehensive experimental program was instigated for intact veined andesite from the El Teniente Mine (Chile). As part of this thesis, high quality specimens were collected from the mine site and characterised using the developed Discrete Vein Network methodology. The selected rock specimens were tested under triaxial compression. The experimental program established that the veins acted as weak mechanical components in the specimens, promoting rock fracture under stress. The experimental results indicated that the stress thresholds identifying the onset of dilatancy and the transition from the stable to unstable fracturing were higher in intact veined rock than in intact rock. The observed behaviour illustrated a significant departure from intact rock where the onset of Acoustic Emissions (AE) correlates with the onset of specimen dilatancy. The
experimental results from intact veined rock indicated that the onset of AE correlated with the

crack damage stress. The departure from the behaviour of intact rock is attributed to the presence

of veins and has significant implications on the behaviour of intact veined rock.

A methodology was developed for numerical simulations of the laboratory experiments on

intact veined rock using the Synthetic Rock Mass (SRM). A contribution of this thesis was the
development of two sets of algorithms to capture the mechanical behaviour of intact veined rock.
The first approach was based on the use of clumped Bonded Particle Models (BPM). Subsequently
the Bonded Block Model (BBM) approach was further developed and implemented. This approach
was shown superior in modelling and validating the intact component of veined rock. The
developed numerical approaches were successful in capturing and reproducing the observed
laboratory behaviour of intact veined rock. The laboratory and numerical investigations made a
significant contribution in characterizing the role of veins in defining the behaviour of intact veined
rock.
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Chapter 1
Introduction

1.1 Problem

Our understanding of how the presence of mineral veins affects the behaviour of rock under load is currently limited. Unlike the influence of joints, the effects of mineral veins on the strength of rock masses have not received considerable attention in rock engineering. The potential influence of veins on rock mass strength is often overlooked.

The geomechanics discipline lacks comprehensive engineering tools that would allow one to efficiently analyse behaviour of veined rock systems. Current widely-used methods of characterization and strength prediction were developed for jointed rock masses and are found to be inadequate when applied to veined rock masses as they are ill-equipped in addressing the particularities of veining. The only rock mass classification system that considers effects of veins, the Laubscher and Jakubec (2001) iteration of the Mining Rock Mass Rating (MRMR), is complex, ambiguous, and lacking rigorous verification in the veined rock.

1.2 Veined Rock Masses and Their Role in Mining

Mineral veins are geologic features resulting from the deposition of minerals from fluids circulating through openings within the rock mass (Best 2003). Veins can range from few centimeters to hundreds of meters in length, and their thicknesses can vary from fractions of a millimeter to several metres. Veins tend to be irregular in shape, often pinching and swelling (Best 2003). Figure 1-1 shows examples of veins at different scales.

A rock mass is a combination of blocks of intact rock and joints that define them. When joints are few and widely-spaced, a rock mass is described as massive. A rock mass, dominated by mineral-filled veins, can have a significantly different behaviour under stress than a rock mass dominated by joints. Unlike joints, veins may exhibit high tensile strength. Geometric properties and mineral composition of veins have been found to affect the rock mass strength (Brzovic 2010; Laubscher and Jakubec 2001).
Veined rock masses are commonly found in porphyry copper deposits in which a system of veins, called *stockwork*, is a dominant feature. Porphyry mineral deposits are the main natural sources of copper, molybdenum, and other metals. Some of the world’s largest metal mines are producing from porphyry deposits. Examples of large mines producing from porphyry deposits include El Teniente, Chuquicamata, and Collahuasi mines in Chile, Bingham Canyon and
Henderson in the USA, Northparkes and Cadia-Ridgeway in Australia, Ok Tedi in Papua New Guinea, Grasberg in Indonesia, and Oyu Tolgoi in Mongolia.

Successful operation of a caving mine traditionally relied on the presence of joints for rock mass breakdown (Brown 2003). Competent rocks pose significant challenges for caving. At El Teniente (Chile), the largest underground Cu-Mo mine in the world, the presence of mineral veins had been found to play a key role in promoting caving in its massive, sparsely jointed rock mass (Brzovic 2010).

The recent research on the behaviour and strength of veined rock has been mostly qualitative. Laubscher and Jakubec (2001) introduced modifications to the original Mining Rock Mass Rating (MRMR) classification system to account for the presence of veins. Brzovic (2010) studied how mineral-filled veins contributed to the primary fragmentation of the massive primary copper ore at the El Teniente mine (Chile). More quantitative studies are necessary in order to improve our understanding of how veined rock behaves under load.

Studying the effects of veining on the rock behaviour and its strength is key for mass mining projects in massive veined rock masses. Veins influence the initiation and propagation of caving, as well as secondary fragmentation of caved material. Understanding strength of veined rock is also key to safe operation of deep mines. The research presented in this thesis aims to contribute to the knowledge that needs to be developed to address these problems.

### 1.3 Objectives

Intact veined rock is a constitutive component of a veined rock mass. Consequently, the behaviour of a veined rock mass depends on the behaviour of its intact blocks. Understanding of how intact veined rock behaves under load is key to developing an understanding of the veined rock mass behaviour.

The primary objective of this work was to develop an understanding and characterize the behaviour of intact veined rock under compressive loads. The secondary objective was to develop an approach for numerical modelling of intact veined rock.
1.4 Methodology

Following a literature review and in consultation with the industrial mining partners it became evident that there was an absence of significant technical literature on the behaviour of veined rock masses. The El Teniente mine in Chile was identified as a host of andesite veined rock whose presence had a significant impact on the mining operation.

A common approach to study the behaviour of intact rock under stress is to conduct laboratory compression tests on cylindrical rock specimens (Fairhurst and Hudson 1999; ISRM 1983). A site visit was undertaken at the El Teniente mine. The purpose of the visit was to get first-hand exposure to the rock mass and underground conditions at the mine and to select rock specimens for laboratory testing. The idea of rock testing of specimens from El Teniente in a laboratory at the University of Toronto was discussed with mine personnel. Large cores of veined mine andesite, the main host of mineralization at the mine, were sent from El Teniente to the University of Toronto for testing.

Ten core specimens, measuring 50 mm diameter and 125 mm long, were prepared and tested at the Rock Fracture Laboratory (RFL) at the University of Toronto. Two sets of experiments were conducted. Comprehensive characterization of each intact veined specimen was carried out. The characterization included the following components:

- Detailed photographic record of the specimen prior to and after the experiment;
- Mapping of surface vein exposures and construction of the vein network in 3D;
- Characterization of vein geometry;
- Mapping of fracture surfaces after the experiments; and,
- Preparation of thin sections following the experiment and petrographic analyses of vein mineralogy.

Triaxial compression experiments were accompanied by acoustic emission (AE) monitoring and measurements of propagation velocities of elastic waves in longitudinal and diametric directions of the specimen. Both provided additional information on the fracturing of the specimens during their compression.
Each experiment was modelled numerically based on the Synthetic Rock Mass (SRM) methodology. SRM was selected for modelling because it allows to capture two main components of intact veined rock: intact fragments and veins that separate them. The SRM approach was critically evaluated for its applicability to numerical simulations of intact veined rock in compression. Numerical modelling also provided additional information with respect to the behaviour of intact veined rock.

1.5 Thesis Structure

The thesis is organized into 10 chapters. Chapter 1 (this chapter) defines the research problem, states the objectives, and presents the methodology for the research.

Chapter 2 introduces the block caving mining method. The need of the mining industry to understand the behaviour of veined rock under stress is illustrated. The chapter describes characteristics of veined rock, common relationships found in the vein systems, and tools that we currently use to characterize the rock mass dominated by veins.

In Chapter 3, background information on the El Teniente mine and its veined rock mass is provided. The chapter includes a detailed geological and mineralogical description of the specimens used in the triaxial experiments.

Chapter 4 deals with the characterization of veins in the specimens. A procedure used to develop discrete vein networks is described. The chapter provides a summary on vein mineralogy and geometrical characteristics.

Chapter 5 provides background information on general behaviour of intact rock and intact andesite in compression. The chapter describes the laboratory experiments in detail, including testing strategies, equipment, procedures, and approaches to data processing.

Chapters 6 and 7 present the results of the two laboratory testing campaigns. The results of the experiments are discussed in the light of the experimental research on progressive failure of intact rock and intact andesite, outlining similarities and differences between intact and intact veined rocks.
Background information on the Synthetic Rock Mass modelling philosophy and its application are given in Chapter 8 of the thesis. The chapter demonstrates the rationale for selecting the SRM as the main methodology for numerical modelling. The chapter provides theoretical background on the Bonded Particle and Bonded Block modelling strategies within the SRM framework, which were both used in this work.

The developed methodologies to undertake the numerical investigations are presented in Chapter 9. Laboratory experiments on intact veined rock specimens were modeled based on the SRM methodology using the Particle Flow Code in 3D (PFC3D) and 3D Distinct Element Code (3DEC) software packages.

Chapter 10 presents the key contributions of this thesis that capture the distinct behavior of intact veined rock under load. The significant practical implications of the research findings are outlined. Finally, the limitations of the present work are acknowledged and recommendations for future work are made.
Chapter 2
Block Caving, Veined Rock and the Need for Systematic Research of the Veined Rock Behaviour

2.1 Introduction

This chapter provides background information on block caving, highlighting its importance in mining of large porphyry deposits. The mechanism of the block caving mining method, its key geomechanical aspects, variations, and current challenges are described. The chapter introduces veined rock masses, which are typical for porphyry systems. Characteristics of veined rock and our current understanding of how mineral-filled veins affect the behaviour of veined rock masses are presented.

In mining, veined rock masses are generally associated with caving (Laubscher and Jakubec 2001). Hence, caving has been the main driver behind the research related to the behaviour and strength of veined rock. Still, very few researchers have investigated the topic of veined rock. Unlike the effects of joints, the effects of mineral veins on the strength of rock masses have not received considerable attention in rock engineering, and we still do not clearly understand how veins affect rock mass cavability, fracturing, and secondary fragmentation.

2.2 Block Caving

Being naturally large, porphyry deposits are well-suited for development using mass mining techniques, called so due to their large scale and high tonnage mining (Brown 2003). Open pit mining is preferred for deposits located near surface. Underground mass mining techniques include block and sublevel caving and their derivatives. Block caving is the only underground mining method that has potential to approach the scale of an open pit mine (Brown 2003). For example, the El Teniente mine, which is operated by Codelco (Chile’s national copper mining company), is currently mining close to 140,000 tonnes of copper-molybdenum ore per day using caving (Brzovic 2010) and its new mine level, which is currently being designed, is expected to produce 130,000 tonnes daily (Hormazabal et al. 2014).

Block caving is an underground mining method that relies on gravity to fracture unsupported ore and transport it to pre-constructed drawpoints for extraction. Because of its high
productivity and low operating costs, the method is attractive to use, and for lower grade deposits, block caving may be the only economically-viable mining option.

Caving refers to rock mass fracturing and disintegration under the effects of stress and gravity. In most situations, caving is unwelcome as it indicates structural failure of the rock mass, and efforts are made to prevent it. In block cave mining, however, caving of ore is highly desired and encouraged as it represents the main mechanism of ore production and the success of the extraction method depends on continuous, predictable breakdown of the orebody rock.

Figure 2-1 illustrates the modern approach to block caving. Caving is initiated by undercutting a block of ore. Undercutting refers to creation of a relatively thin horizontal slice at the base of the block. The mine level on which the undercutting is carried out is called the undercut level or the undercut. Undercutting removes the vertical support of the ore column above, and when a large enough void is created, the ore begins to cave due to ground stresses and gravity.

Figure 2-1. Illustration of the block caving mining method (Atlas Copco 2007).

Fragmented or caved rock is collected by inverted cone shaped openings acting as funnels, called drawbells, located in the floor of the undercut level (see Figure 2-1) and channeled to the
production level located below the undercut. Mechanized loaders haul fragmented ore from drawpoints, located at the bottom of the drawbells on the production level, to ore passes used to transfer the ore to the haulage level below where it is taken away for processing (see Figure 2-1).

As caved material is removed on the production level and provided that the conditions are suitable for caving to propagate, caving continues to progress upwards, away from the initial undercut. The caving front eventually breaks through the ground surface, resulting in surface subsidence.

Being a highly-productive mining method, block caving requires significant upfront capital for development of infrastructure and long lead time for undercutting. Consequently, risks associated with implementation of the method are high and any potential errors are costly. Because block caving relies on the natural ability of the orebody to cave, geomechanics plays a crucial role in the success of a caving operation. Laubscher (2001) lists 25 parameters that should be evaluated and addressed in the design of a block caving mine. Every single one of them has at least one component related to geomechanics. This is further reinforced by Brown (2003) who describes 10 major risk factors associated with the design and operation of a block caving mine; geomechanical conditions are a major component in all.

Any rock mass will cave provided that an undercut of sufficient size is created (Laubscher 2001). However, to be successful, caving needs to remain sustainable over the lifetime of the operation. Brown (2003) lists key factors influencing initiation and propagation of caving:

- Stresses induced in the back of the cave;
- Strength of the rock mass; and
- Geometry and strengths of the discontinuities (e.g. joints) in the rock mass.

Brown (2003) describes two types of caving mechanisms. The first one is known as gravity or stress release caving. Under this caving mechanism, compressive tangential stresses in the back of the cave are low or tensile, and blocks of rock become free to detach by falling or by sliding on inclined discontinuities under the influence of gravity. Brown (2003) points out that presence of a well-developed shallow-dipping discontinuity set is highly desired for trouble-free caving. Brown (2003) states that low horizontal in-situ or induced stresses would create conditions suitable for stress release caving mechanism to be in operation.
The second form of caving described by Brown (2003) is stress caving. Stress caving may occur when the induced tangential stresses in the back of the cave are high in comparison to the compressive and shear strengths of the rock mass and the shear strength of the discontinuities. As the result, rock mass failure occurs at the boundary of the cave and the rock blocks detach under the influence of gravity (Brown 2003). Duplancic and Brady (1999) describe the main mechanisms of failure being slip on existing joints and cavity collapse by rock fall. They confirmed that a flat-dipping discontinuity set is required for release of blocks at the boundary of the cave. Brown (2003) also adds brittle fracture of intact rock as another viable mechanism of failure related to stress caving.

Based on undercutting strategy, three variants of the block caving mining method are recognized: post-undercut (or conventional), pre-undercut, and advanced undercut (Figure 2-2). In the post-undercut approach, the development of the undercut level follows the development of the underlying extraction level. In the pre-undercut strategy, the undercut is developed ahead of the infrastructure on the extraction level. The distance by which the development of the extraction level infrastructure lags the development of the undercut is often equal to the separation distance between the two levels: the 45° rule. In the advanced undercut approach, the undercutting is carried above a partially completed section of the extraction level. Typically, extraction drifts are developed on the extraction level, and the development of the drawbells is delayed, adhering to the 45° rule (Brady and Brown 2005; Brown 2003).

![Figure 2-2. Undercut development strategies used in block caving.](image)

Brown (2003) points out that presence of discontinuities within the rock mass plays an important role for successful initiation and propagation of a cave front. Presence and spatial
arrangement of natural discontinuities also have direct influence on rock fragmentation characteristics of the deposit, which Brannon et al. (2011) called being “perhaps the single most important geotechnical feature”, as “it impacts most aspects of mine design and operation.”

Because natural rock structure plays an important role in the success of caving, caving methods have been traditionally applied to deposits in weak to moderately-strong jointed rock masses at relatively shallow depths (Brannon et al. 2011; Flores 2014). Such rock masses are often referred to as secondary formations or secondary ore, following the mining nomenclature of Codelco (Chitombo 2010). As secondary ore type deposits have been becoming depleted, caving operations have been moving deeper, into more competent massive rock masses, which are commonly called primary formations or primary ore. Nowadays, the majority of mechanized block caving operations are in primary rock formations (Chitombo 2010).

According to Flores (2014), attempts to exploit primary ore type deposits by caving began in the 1970s at some of the mines in Chile. It was realized that the technical experience of caving secondary ore was insufficient for successful mining of lower grade and more competent ore bodies. Consequently, the cave mining has been undergoing transformation necessitated by new technical and economic challenges. Advances, improvements, and innovations made in the past 30 years are significant, but technical knowledge and understanding of caving mechanics and cave engineering have often lagged behind industrial requirements (Brown 2004; Flores 2014).

Nowadays, mining companies are expected to deal with even tougher challenges. The industry is now facing harder rock, at much greater depth, under high in situ stress, and requiring larger footprints and block heights to be mined economically. These conditions remain outside of past and current practices (Flores 2014), while challenges related to caveability, fragmentation, seismicity, and support of ground have become more apparent.

Our time-honoured experience says that massive rock masses are not suitable for caving, yet massive porphyry rock caves and the presence of veins appears to be one of the reasons why it is so (Brzovic 2010). The mining industry needs to develop greater understanding of how veins alter the behaviour of massive rock.
2.3 Veined Rock

Based on the relative strengths of veins and of host material, veined rock can be divided into two categories. The host material can have higher strength than the veins. Opposite can also be true – weak host with strong veins. Porphyry-type rock masses fall under the first category, and this is the type of rock that is examined in this thesis.

2.3.1 Origin and Characteristics of Systems of Mineral Veins

Bons et al. (2012) define veins as mineral aggregates formed by precipitation from fluids in dilatational sites. When rock mass fractures, resulting cracks dilate, creating space necessary for minerals to grow into, forming veins. Therefore, formation of veins is closely associated with rock mass fracturing.

Porphyry systems form over large plutons, deep (5-15 km) subterranean bodies of magma intruded into pre-existing rocks, ascending towards surface in the form of porphyry stock intrusions (Sillitoe 2010). Porphyry Cu/Mo deposits are formed by hydrothermal processes when metal-bearing hot pressurized fluids escape the water-rich magma into the host formation (Sillitoe 2010). The escaping fluids travel in part through existing rock fractures and in part by creating new paths through hydraulic fracturing (Bons et al. 2012; Davis and Reynolds 1996). Loss of pressure and reduction in temperature promotes deposition of minerals and crystal growth, sealing opened fractures. In a porphyry copper deposit, formation of the vein stockwork is not a single isolated event but a lengthy process involving numerous episodes of intrusions, cracking and sealing of existing and development of new veins, under multiple stress and chemical scenarios (Bons et al. 2012; Davis and Reynolds 1996; Vry et al. 2010).

Veins vary in shape, spacing, thickness, length, and mineral composition. They can also be distinguished on the basis of character of mineral growth and on their age. Veins tend to be lenticular in general (Johnston and McCaffrey 1996). Smaller veins tend to display simple geometries with smooth outlines, while larger veins often show complicated geometries involving disturbed geometry, merged en échelon structures, and serrated margins (Johnston and McCaffrey 1996).
Vein thickness can vary from sub-millimeter to tens of meters (Loriga 1999). Cumulative distribution of vein thicknesses within a population normally follows a power-law distribution (André-Mayer and Sausse 2007; Gillespie et al. 1999; Loriga 1999; McCaffrey et al. 1993):

\[ N_T = C T^{-D} \]  

(Eq. 2-1)

where \( N_T \) is the cumulative number of veins having thickness greater than or equal to \( T \), \( C \) is a coefficient of proportionality, and \( D \) is a scaling factor. When plotted in the log-log space, the relationship 2.1 produces a straight line with a negative slope \( D \). A series of generic power-law plots are shown in Figure 2-3 to illustrate how parameters \( C \) and \( D \) affect the position and slope of the curve. \( C \) represents the cumulative vein frequency at the thickness of 1 unit. The scaling factor \( D \) represents relative proportions of thin veins with respect to thick ones. Higher values of \( D \) (steeper slope) indicate that a population contains a high number of thin veins in comparison to thick ones (Gillespie et al. 1999).

Figure 2-3. Illustrations of the power-law relationships with different values of \( C \) and \( D \) parameters.

Analyses of vein thickness data by Gillespie et al. (1999) based on 30 line samples from 6 outcrops and 3 mine localities showed that \( D \) values were consistent at 0.8. Loriga (1999) established \( D \) value of 0.82 based on 0.1-40 cm thick veins observed in drill core and 0.66-0.88...
range for stockwork observed in outcrop in the Guanajuato mining district in Mexico. Results of the work by André-Mayer and Sausse (2007) on veins of the Rosia Poeni (Romania) porphyry copper deposit showed $D$ values ranging between 1.1 and 1.7.

Johnston and McCaffrey (1996) showed that vein lengths in a population were related to their thicknesses and that the relationship between the two also followed the power-law rule:

$$L = kT^a$$

(Eq. 2-2)

where $L$ is vein length (mm), $T$ is vein thickness (mm), $k$ is a coefficient of proportionality, and $a$ is a scaling factor.

On the scale of a thin section to an outcrop, the scaling parameter $a$ is less than 1 and in most geological settings lies between 0.6 and 0.8 (Johnston and McCaffrey 1996). For small, thin section scale veins, Johnston and McCaffrey (1996) determined that $k$ varied between 20 and 2000.

Figure 2-4 illustrates power-law relationships of Equation 2-2 for $k$ value of 100 and $a$ values of 1 and 0.6. Johnston and McCaffrey (1996) noted that as $a < 1$ veins grow thicker faster than they lengthen and that vein axial ratios increase with vein size. The vein geometry, therefore, tends to be self-affine, changing exponentially with scale (Johnston and McCaffrey 1996).

Based on the mathematical relationship (Equation 2-2 and Figure 2-4), $k$ represents the lengths of the vein of 1 mm thickness. In physical terms, Johnston and McCaffrey (1996) note that $k$ is a function of wall rock strength and strain field. Small $k$ values are indicative of low axial ratios and correspond to veins formed by simple shear in softer rock, while pure extension in hard rock tends to result in high vein axial ratios and high $k$ values (Johnston and McCaffrey 1996).

Vein mineral composition can vary widely. It is a function of the source of the hydrothermal fluids, depositional environment, and the host rock. Quartz is the most common vein mineral (Philpotts 1990), and quartz veins tend to be most abundant in copper porphyry systems (Brzovic 2010; Gruen et al. 2010). Being relatively insoluble under surface conditions, solubility of quartz increases substantially under high temperature and pressure (Philpotts 1990).
Veins are classified according to their morphology. Syntaxial and antitaxial vein types are recognized (Davis and Reynolds 1996). Where mineral crystals grow from the walls of the vein towards the centre, the vein is termed syntaxial. In an antitaxial vein crystals grow from the centre of the vein toward the wall rock (Bons et al. 2012; Davis and Reynolds 1996).

### 2.3.2 Current Understanding of Veined Rock Masses

In mining geomechanics, estimation of strength of the rock mass and reliable forecasting of its behaviour in response to mining are of most interest and difficulty. Both behaviour and strength of the rock mass depend on the behaviour and strengths of blocks of intact rock and discontinuities that separate the blocks. In veined rock masses, the complexity of these factors are magnified by the presence of veins. On the block scale, the veins influence the strength of intact material. Veins of the rock mass scale have potential to influence the strength of the rock mass.

Traditionally, the strength of the rock mass is derived based on degradation of intact rock properties. Intact strength is established using laboratory compression tests. Intact scale properties are reduced based on rock mass characteristics. The latter are established using one of rock mass
classification techniques which is often empirically linked to a failure criterion (e.g. Hoek and Brown 1997).

Unfortunately, widely-used rock mass classification systems (RMR and Q) are not equipped to adequately address veined rock masses. Both the Rock Mass Rating or RMR (Bieniawski 1989) and the Tunneling Quality Index or Q (Barton et al. 1974) systems have been developed based on tunneling case studies from jointed rather than veined rock masses. The RMR system contains no reference to veins (Bieniawski 1989). In the Q system, the veined rock is accounted for through the joint alteration parameter (Ja). The Q system recognizes that presence of “tightly healed” joints or ones containing “hard, non-softening, impermeable filling” is a factor that improves rock mass quality in comparison to the same rock mass with “unaltered joints” (Barton et al. 1974).

The only current rock mass characterization scheme that considers the effects of mineral veins is the Mining Rock Mass Rating (MRMR) system. Prior to its latest modification, the system did not account explicitly for presence of veins (Laubscher 1990). In the current version (Laubscher and Jakubec 2001), the system accounts for potential effects of “cemented joints” and “veins”, which are treated separately, by considering their frequency of occurrence and hardness of the infilling or cementing material. The details of the MRMR system are presented in Appendix A.

Brzovic (2010) proposed that mineral veins were key to the disassembly of the massive veined primary ore rock mass at the El Teniente mine during caving and subsequent draw. Through characterization of veins in situ and by observations of caved rock blocks in the drawpoints Brzovic (2010) concluded that the rock fragmented predominantly along “weak” veins, which he determined had two characteristics:

- Vein infill contained less than 1/3 of hard minerals by volume, and
- Veins were 2 mm or more thick.

Hard minerals were defined to have Mohs hardness greater than 4. Brzovic (2010) noted that there was no relationship between the vein infill and vein thickness, claiming that the parameters were independent. Brzovic (2010) also postulated that shearing through veins was the most common failure mode during caving and secondary fragmentation at El Teniente.
The characteristics for the definition of weak veins proposed by Brzovic (2010) are useful in characterization of massive veined rock masses and assessment of their potential fragmentation. Based on field observations, Brzovic’ results demonstrated the assumption of Laubscher and Jakubec (2001) that competence of the vein infill governs the strength of veins and consequently affects the strength of the rock mass.

The Brzovic’ observations and conclusions regarding vein thickness are somewhat arbitrary, however. His recorded observations of veins with 2 mm or more thickness bounding fragmented rock blocks represented the majority of observations, the average of 42% (Brzovic and Villaescusa 2007). In comparison, veins with the thicknesses of 1-2 mm and <1 mm amounted to 29% average each, which can be considered significant. Because vein thickness and vein lengths are related (see Section 2.3.1), perhaps, the observations of Brzovic (2010) can be better explained by using vein lengths than thicknesses.

Brzovic (2010) showed that thicknesses and lengths of veins mapped in mine drives at El Teniente generally agreed with the power-law rule described by Johnston and McCaffrey (1996) (see Section 2.3.1). Based on the data of Johnston and McCaffrey (1996), a 1 mm thick vein can have a length between 0.02 m and 2 m. Veins of 1 mm thickness mapped by Brzovic (2010) had a range of approximately 0.4 m to 4 m. Veins with thickness of <1 mm ranged between 0.2 m and 1.2 m in length, over 2 mm thick – 0.4-10 m in length.

Conclusions by Brzovic (2010) regarding thickness of “weak” veins were made based on observations of blocks of caved material. Longer intersecting veins tend to form larger (and heavier) blocks than shorter veins. If the infill strength is identical in all veins, then it would be intuitive to conclude that larger blocks would detach more readily than smaller ones during caving. Therefore, vein length or persistence would be a more logical parameter than vein thickness in controlling rock mass strength and fragmentation. The idea of vein persistence affecting rock mass strength is in agreement with the concept of “cemented joints” of the MRMR system of Laubscher and Jakubec (2001).

2.4 Conclusions

Block caving can be an attractive and cost-effective underground mining method, provided that rock mechanics conditions are favourable for its application. As deposits with ground
conditions that have been traditionally considered being key for caving – jointed rock mass typical of secondary ore in a moderate stress environment – are becoming depleted, caving is being applied to hard, massive rock under high in-situ stresses, which are characteristics of deep orebodies. Our current experience of caving in such conditions is limited (Flores 2014).

Porphyry systems, which are major sources of copper and molybdenum, are characterized by presence of mineral-filled veins, termed stockwork. Experience with caving (e.g. Brzovic 2010; Laubscher and Jakubec 2001) suggests that in massive, sparsely-jointed rock masses presence of veins is important for rock mass disassembly. Laubscher and Jakubec (2001) and Brzovic (2010) formulated several conditions that they believed affected the strength of a veined rock mass:

- The rock mass strength is affected by the presence of veins of multiple scales. Veins on the scale of a core specimen influence intact rock strength. Strength of rock blocks depends on the strength and density (frequency) of veins in the block. Block-defining veins, termed “cemented joints” by Laubscher and Jakubec (2001), influence the strength of the rock mass; again, vein strength and density seem to play a role.
- On all scales, veins act as strength-weakening elements. Strength decreases as the density of veining increases.
- Hardness of the vein infill affects the vein strength, and vein mineralogy plays a defining role. Presence of harder minerals, such as quartz, make veins stronger. Presence of softer minerals weakens veins. Vein infill is a multi-mineral assembly and should be considered as such.

Our current understanding of specific veins characteristics that influence the rock mass strength and fracturing is limited and is largely based on conjectures and indirect evidence rather than systematic experimentation and direct observations. Works by Laubscher and Jakubec (2001) and by Brzovic (2010) were of qualitative and observational nature. Currently, there is need for quantitative, experimental work on the subject. Because behaviour of veined rock is complex and because field-scale or in-situ experiments are expensive, poorly-controlled, and often unpredictable, laboratory experiments and numerical simulations are better-suited for studying veined rock at this time.
This research focused on studying the behaviour of intact veined rock specimens using laboratory testing and numerical simulations. The next chapter outlines how specimens for the experiments were selected, describing source material, its generic properties, and the process followed in preparation of the specimens.
Chapter 3
El Teniente Mine and Selection of its Veined Mine Andesite Specimens for Experimental Work

3.1 Introduction

This chapter provides detailed information on the specimens from the El Teniente mine that were chosen for the experimental work. The chapter gives a brief description of the El Teniente mine, including geology of the deposit, the mining method, in-situ stress conditions, and the past and ongoing challenges of the mining operation. The mine details are followed by a description of the specimens selected for the experiments, including their origin, preparation details, and mineralogical composition of host material. Detailed characterization of veins is provided separately in Chapter 4.

Veined mine andesite of the El Teniente’s Mafic Intrusive Complex (El Teniente mine, Chile), often abbreviated as CMET (Spanish for Complejo Máfico El Teniente), was selected for the laboratory experiments. El Teniente’s mine andesite was an ideal rock type for this research as it fits the target category of rock that this research aims to study. CMET represents the primary ore at El Teniente. Brzovic (2010) described El Teniente’s andesite as massive, with very few open discontinuities, characterized by the presence of stockwork.

3.2 El Teniente Mine

3.2.1 Background

El Teniente (‘The Lieutenant’) is the largest underground block cave mine in the world, producing copper and molybdenum (Brown 2003). It is located in the Central Andes, approximately 70 km SSE from Santiago, Chile. The orebody outcrops on a side of a mountain, which reaches 3600 m elevation above the sea level. Current mining production takes place approximately at the elevation of 2200 m (Figure 3-1). The extent of mineralization has been confirmed to a depth of approximately 1300 m, with true mineralization depth still unknown (Brzovic 2010).
The El Teniente’s orebody has been in continuous production since 1906. More than 1100 million tonnes of ore have been mined since (Araneda et al. 2004). Nearly half of the total ore produced to date was extracted during initial 70 years. Production of another 500 million tonnes took place between 1975 and 1995 (Brzovic 2010). It is estimated that a pre-mining resource at El Teniente was over 12 billion tonnes of ore averaging 0.65% copper and 0.019% molybdenum (Vry et al. 2010).

The major mine infrastructure is located within the Braden Pipe. The mine is accessed through an adit at elevation of 1983 m, at the Teniente 8 level (Araneda et al. 2004; Brown 2003). Mining has occurred on different levels from various parts of the orebody – called mine sectors – surrounding the Braden Pipe (see Section 3.2.2). Figure 3-2 illustrates locations of the operating and future sectors. Currently, El Teniente produces approximately 140,000 tonnes of ore per day (Brzovic 2010). A new mine level (NML) is now under development. It is scheduled to start production in 2017 and is expected to eventually produce 130,000 tonnes of copper-molybdenum ore per day (Hormazabal et al. 2014). The new mine level is located below the Teniente 8 level (see Figure 3-2) and is projected to contain 5 mine sectors (Yanez 2004).
Figure 3-2. Isometric view of the El Teniente mine showing main current and future mine sectors (courtesy of Codelco).

3.2.2 Deposit Geology

The orebody at El Teniente is situated around an 800-1000 m diameter chimney of sub-volcanic breccia known as Braden Pipe, reaching distances between 400 m and 800 m away from the perimeter of the pipe (Figure 3-3). The Braden breccia, emplaced largely post-mineralization, is barren (Vry et al. 2010).
Figure 3-3. Plan view showing simplified El Teniente mine geology at the Teniente 6 level, 2165 m elevation (after Brzovic 2010; Vry et al. 2010). The grid represents the mine grid, which is oriented 14°19’34” East relative to geographic north (marked with an arrow).
Besides the Braden breccia, the main rock types at El Teniente are andesites, diorites, and breccias (Figure 3-3 and Figure 3-4). Mineralisation is mostly (approximately 80%) hosted by the mine lithology called the *Mafic Intrusive Complex* (CMET), also referred to as *Teniente Mafic Complex*, the oldest and most abundant rock unit at the mine (Brzovic 2010; Vry et al. 2010). CMET is a complex collection of gabbros, diabases, basaltic and basaltic andesite porphyries. CMET is also known as *andesite* or *mine andesite* (Brzovic 2010; Cannell et al. 2005).

![Figure 3-4. Vertical cross-sections looking north at 200N (left image) and 600N (right image) showing simplified geology at El Teniente (after Vry et al. 2010). Cross section locations and colour legend are shown in Figure 3-3.](image)

Visually, CMET is of dark green and/or black colour, with aphanitic, porphyritic, and coarser, equigranular textures (Vry et al. 2010). CMET is classified as mafic to intermediate, with 46 to 57 % SiO₂ by weight (Vry et al. 2010). Cannell et al. (2005) described CMET as “a sill and stock complex that intruded andesitic lava flows and volcanoclastic units of the Farellones Formation”.

CMET itself contains a number of intrusions. Besides the Braden breccia, the El Teniente’s Mafic Intrusive Complex was intruded by a number of felsic-intermediate plutons and breccias of igneous and hydrothermal nature (Vry et al. 2010). Two main intrusions are the *Sewell Tonalite*, locally known as *Diorite Porphyry* or *Quartz Diorite* (Brzovic 2010; Vry et al. 2010), and *Dacite Porphyry* (Figure 3-3).
The Sewell Tonalite is a large sub-vertical intrusive body located east and southeast of the Braden Pipe (Figure 3-3 and Figure 3-4). It is the oldest felsic to intermediate intrusion at El Teniente, being emplaced prior to mineralization (Vry et al. 2010). Sewell Tonalite is characterized by pervasive sericite alteration in the upper levels and minor biotite alteration at depth (Vry et al. 2010).

The Dacite Porphyry is a 300 m wide tabular north-south trending felsic intrusive body (Brzovic 2010; Vry et al. 2010). It extends nearly 1.5 km away from the Braden Pipe, beyond the extent of mineralization (Figure 3-3 and Figure 3-4).

The Braden Pipe is the largest breccia unit at El Teniente (Figure 3-3 and Figure 3-4). The breccia material is grey, massive, visually resembling concrete. The pipe has a shape of an inverted cone, with walls dipping typically at 60-70°. On the east side, the contact is sub-vertical (Brzovic 2010). Other breccia bodies at El Teniente are mostly associated with felsic intrusions (Vry et al. 2010).

According to Brzovic (2010), fault systems at El Teniente mainly consist of NE and NW trending strike slip faults. Two main structures have been recognized on the mine scale. Fault P is located in the south part of the deposit, and Fault N1 in the north (Figure 3-3). Both faults had been traced over distances of 800 m horizontally and 400 m vertically. The faults contain gauge infill with average thickness of 10 cm (Brzovic 2010).

Primary ore at El Teniente is massive, and open joints are rarely found. Small-scale local faults have low frequency of occurrence, recorded approximately 0.1/m in line mapping. The main structural feature of the primary ore is a high-frequency network of small-scale mineral veins, stockwork (Brzovic and Villaescusa 2007). The stockwork of the mine andesites (CMET) and dacite intrusions is the main host of copper and molybdenum mineralization at El Teniente (Cannell et al. 2005). Veining had been associated with various alteration stages (Brzovic 2010; Vry et al. 2010).

3.2.3 Mining Method

Block caving has been used at El Teniente from approximately 1940, first for mining of secondary ore, which was weak and fragmented readily, and later at deeper primary ore sectors. Mechanized caving using tracked loaders (LHDs) was introduced on the Teniente 4 level in 1982.
(Chacon et al. 2004). Initially, caving was carried out using the post-undercut approach. However, problems associated with increased seismicity and damage to the extraction level led to the adoption of the pre-undercut approach.

A typical extraction level layout, which became known as ‘El Teniente layout’ (Brown 2003), used at the mine is illustrated in Figure 3-5. Drawpoint drifts are straight, oriented at 60° to the production drifts. Production drifts are on 30 m spacing. The undercut level is located 18 m above the production level. The block height typically varies between 110 and 220 m (Brzovic 2010).

![Figure 3-5. Typical El Teniente’s extraction level layout (after Chacon et al. 2004). Dimensions are in metres.](image)

### 3.2.4 In-Situ Stress

Brzovic (2010) reported that in-situ stress measurements at El Teniente have been conducted in more than 150 locations. Due to complex geometries created by natural topography and old and current cavities, any measurement of regional stress field is difficult. All
measurements carried out to date likely represent local rather than global conditions (Brzovic 2010). Cavieres (2011) reported that the magnitude of the major principal stress at the Esmeralda sector was 45 MPa. At the NML, the major principal stress was estimated to reach 60 MPa (Yanez 2004).

Brzovic (2010) reported on recent estimates of stress being carried out at the Western Australia School of Mines, Curtin University, using the acoustic emission technique on diamond drill core from deep holes. These results are shown in Figure 3-6.

The measurements show that the major principal stress at El Teniente is sub-horizontal, aligned subparallel to the geographic NS direction. The intermediate stress, also sub-horizontal, is oriented approximately EW. The minor principal stress is sub-vertical.

3.2.5 Challenges

Caving of competent primary ore is difficult, and rock mass cavability is a major concern at El Teniente (Chacon et al. 2004; Orellana et al. 2014). Rock mass preconditioning, hydraulic fracturing and confined blasting, has been used to improve rock mass cavability (Brzovic et al. 2014).

Natural fragmentation of primary ore is coarse, and the amount of secondary blasting can be significant at El Teniente (Chacon et al. 2004). Rock mass preconditioning has been used with
success at the Teniente 4 South level to reduce the number of coarse blocks and drawpoint hang ups (Chacon et al. 2004). Use of hydraulic fracturing to improve fragmentation has been ongoing at El Teniente since 2008 (Brzovic et al. 2014). Fragmentation remains a point of concern.

Collapse of openings on the undercut level has been an ongoing problem at El Teniente (Chacon et al. 2004; Orellana et al. 2014). Advance of the mine’s Esmeralda sector ceased in 2010 due to severe collapses developed ahead of the undercutting front. Araneda and Sougarret (2008) reported that the collapses were likely related to the caving face being too wide. Esmeralda was subsequently restarted with 2 separate caves, Block 1 and 2, using the conventional undercutting but with rock mass preconditioning by hydraulic fracturing (Orellana et al. 2014).

With the exploitation of primary ore under increased in-situ stresses El Teniente began to experience mine-induced seismicity with rockbursting (Dunlop and Belmonte 2005; Rojas et al. 2000). A series of seismic events and rockbursts between January 1990 and March 1992 led to a temporary shutdown of the Teniente Sub 6 sector in March of 1992. To manage seismicity at the Teniente Sub 6 level, the mine implemented a microseismic monitoring system and modified the mining strategy, including uniform extraction, limiting of a production rates based on microseismic data analyses, and use of remote controlled equipment (Rojas et al. 2000). Changes from the post-undercut to pre-undercut caving approach were also implemented to help deal with mine induced seismicity. Today, mine seismicity remains a major concern at El Teniente (Chacon et al. 2004).

3.3 Intact Properties of CMET Rock

CMET is characterized as massive, with very few open discontinuities. A dense network of mineral-filled veins is present at different scales, from rock fragments to excavation scale, and possibly exists on a larger scale (Brzovic 2010). The CMET veins are predominantly filled with quartz, anhydrite, and chalcopyrite (Brzovic and Villaescusa 2007). In laboratory experiments involving compression at high stresses the El Teniente’s primary ore generally exhibits violent brittle behaviour (Rojas et al. 2000). Table 3-2 summarizes key intact rock properties of CMET andesite.
Table 3-1. Intact rock parameters of CMET andesite (summarised from Brzovic et al. 2008; Brzovic 2010; Dunlop and Gaete 2001; Windsor et al. 2006).

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density</td>
<td>2800 kg/m³</td>
</tr>
<tr>
<td>Young Modulus</td>
<td>50 - 60 GPa</td>
</tr>
<tr>
<td>Poisson’s Ratio</td>
<td>0.16 - 0.30</td>
</tr>
<tr>
<td>Unconfined Compressive Strength (UCS)</td>
<td>115 - 130 MPa</td>
</tr>
<tr>
<td>Tensile Strength</td>
<td>14 MPa</td>
</tr>
<tr>
<td>Hoek-Brown Constant m₁</td>
<td>9.1</td>
</tr>
</tbody>
</table>

3.4 Description of Laboratory Specimens

3.4.1 Origin

The CMET specimens used in the experiments were extracted on the Teniente 7 level of the El Teniente mine, approximately 122 m below the undercut level of the Esmeralda mine sector, from two orthogonal 150 mm diameter boreholes. The core in the boreholes was not oriented. Figure 3-7 illustrates the El Teniente’s main lithological units on the 2063 level and the borehole locations.
3.4.2 Specimen Preparation

Two 150 mm diameter sections of core were delivered to the University of Toronto for testing (Figure 3-8). Large-diameter core S01 was extracted from the PB-NNM-02 borehole (Figure 3-7), approximately 26 m away from the collar. The S02 core was extracted from the PB-NNM-03 borehole, approximately 7 m away from the collar. Specimens S01 and S02 served as the source from which final laboratory-sized core specimens were prepared.
Figure 3-8. Photographs of two large-diameter core pieces obtained from El Teniente. Specimen S01 was extracted from borehole PB-NNM-02, and Specimen S02 from the PB-NNM-03 borehole.

The preparation of the laboratory specimens involved a number of stages, including:

- Cutting of large-diameter cores;
- Coring of laboratory-sized specimens;
- Specimen polishing; and
- Detailed photography.

A decision was made to core the laboratory-sized specimens axially from the large core pieces. This allowed to maximize the number of test specimens and simplified handling of large-diameter cores. As the first preparation step, large-diameter cores were cut across their long axes into 140 mm long cylinders using a stationary slow-feed diamond circular saw (Figure 3-9).
Laboratory-sized specimens were cored out of rock cylinders using a vertically-mounted hydraulic-feed drill. A thin-wall diamond-impregnated hollow coring drill bit with an internal diameter of 50 mm was used to core the final specimens (Figure 3-10). Nineteen core specimens were extracted in total. The specimens were numbered in sequence (1-19) following the order of their coring.
Figure 3-10. Preparation of 50 mm diameter specimens from large rock cylinders. Coring of the specimens is shown in photograph a). Three specimens were cored from each rock cylinder, b).

The cylindrical specimens were prepared following the height-to-diameter ratio of 2.5, as recommended by the suggested methods of the International Society of Rock Mechanics (ISRM) for testing of intact rock in triaxial compression (ISRM 1978). The Meister V3 CNC high precision grinder was used to prepare specimen ends (Figure 3-11). Both ends of each specimen were machined using a coarse diamond wheel to reach the height of 127 mm. Fine diamond wheel was then used to remove additional 1 mm from each end, to reach the final specimen height of 125 mm. The ends of each specimen were within 0.01 mm of being parallel to each other.
In the final stage of the preparation, each specimen was photographed (Figure 3-12). A reference grid was drawn on the side surface of each specimen. The vertical spacing between the circumferential lines was 25 mm. The vertical axial lines represented an angular separation of 15°. Each vertical line was uniquely identified based on its angular position around the circumference of the specimen (0°, 15°, 30°, etc.). The top and the bottom sides of the specimen were marked with reference to the 0°/180° and 90°/270° principal axes, corresponding to the circumferential values. The position of the 0° vertical line was selected randomly. The purpose of the grid was to aid in reconstruction of the specimen’s surface and positioning of the specimen within the testing cell. Each specimen was photographed from the top and the bottom. Photographs of the specimen sidewall was taken at every 15° of rotation around its long axis.
Figure 3-12. Each specimen was photographed. A high-resolution photograph was taken of the specimen’s surface at each 15° of revolution.

3.4.3 Final Specimens

Ten specimens were selected for triaxial experiments (Figure 3-13). Table 3-2 summarizes specimen origin and orientation with respect to the regional major principal stress ($\sigma_1$) direction. The selection of specimens was governed by various criteria. Specimens with fewer and well-defined discrete veins were preferred to ones having numerous veins and poorly-defined vein geometry. An attempt was made to avoid samples with veins oriented at angles of $45^\circ$-$70^\circ$ to the long axes of core as such orientations are preferential to fracturing during compression testing. Unfortunately, every sample had veins oriented at such angles, and as the result this criteria was not satisfied. Samples with narrower veins were preferred to the ones containing wider veins. Finally, an attempt was made to select samples cored adjacent to each other within the large-diameter core pieces. The rationale for this was that adjacent samples were more likely to produce similar response during testing, leading to repeatable results.
Figure 3-13. Photographs of 10 intact veined andesite specimens that were selected for triaxial testing. Each specimen is 50 mm in diameter and 125 mm long.

Table 3-2. Summary of specimen origin and orientation with respect to the mine major principal stress ($\sigma_1$) direction.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Origin</th>
<th>Orientation wrt $\sigma_1$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Specimen 1</td>
<td>S01</td>
<td>normal</td>
</tr>
<tr>
<td>Specimen 2</td>
<td>S01</td>
<td>normal</td>
</tr>
<tr>
<td>Specimen 5</td>
<td>S01</td>
<td>normal</td>
</tr>
<tr>
<td>Specimen 6</td>
<td>S01</td>
<td>normal</td>
</tr>
<tr>
<td>Specimen 7</td>
<td>S01</td>
<td>normal</td>
</tr>
</tbody>
</table>
3.4.4 Density

Measurements showed that the densities of the specimens were close to the 2,800 kg/m³ value that is commonly reported for CMET (Table 3-2). The measured values varied between 2,820 and 2,890 kg/m³, with an average of 2,850 kg/m³.

3.4.5 Mineralogical Composition

Mineralogical analyses of the tested specimens was carried out after the experiments using thin sections. Thin sections were prepared from disks that were cut perpendicular to the long axes of the specimens. The analyses focused on the mineralogical composition of veins and groundmass. The results of the mineralogical analyses for the groundmass of the specimens are summarized in Table 3-3. Chapter 4 focuses on vein characterization.

Table 3-3. Summary of petrographic analyses of the groundmass of the specimens tested in compression (summarized from O'Shaughnessy and Mailloux (2015)).

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Grain Size (µm)</th>
<th>Volumetric Content (%)</th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Plagioclase</td>
<td>Anhydrite</td>
</tr>
<tr>
<td>Sp. 1</td>
<td>15 - 1750</td>
<td>43</td>
<td>12</td>
</tr>
<tr>
<td>Sp. 2</td>
<td>30 - 520</td>
<td>37</td>
<td>13</td>
</tr>
<tr>
<td>Sp. 5</td>
<td>20 - 1800</td>
<td>49</td>
<td>14</td>
</tr>
<tr>
<td>Sp. 6</td>
<td>30 - 1000</td>
<td>53</td>
<td>8</td>
</tr>
<tr>
<td>Sp. 7</td>
<td>20 - 1575</td>
<td>55</td>
<td>7</td>
</tr>
<tr>
<td>Sp. 8</td>
<td>25 - 5600</td>
<td>39</td>
<td>13</td>
</tr>
<tr>
<td>Sp. 12</td>
<td>20 - 1400</td>
<td>-</td>
<td>5</td>
</tr>
<tr>
<td>Sp. 14</td>
<td>20 - 700</td>
<td>-</td>
<td>5</td>
</tr>
<tr>
<td>Sp. 15</td>
<td>15 - 2010</td>
<td>21</td>
<td>5</td>
</tr>
<tr>
<td>Sp. 19</td>
<td>15 - 1925</td>
<td>12</td>
<td>5</td>
</tr>
</tbody>
</table>
The mineralogical analyses of the groundmass (Table 3-3) highlights some of the differences between the specimens sourced from the S01 and S02 large diameter cores. Specimens sourced from S01 contain more plagioclase and less quartz than the specimens from the S02 core. They also contain no sericite/muscovite, which are often found as alteration minerals often associated with hydrothermal ore veins (Klein and Hurlbut 1993). Larger grains of the host are characteristic of plagioclase. In Table 3-3 ‘other’ components include tremolite, chalcopyrite, bornite, k-feldspar, and magnetite. These, in general, accounted for less than few percent each.

The results of the analyses of the groundmass showed that the mineralogy of the investigated specimens differed from values for andesite reported in the technical literature. Andesite is a generic rock name whose mineralogy can vary, which is reflected by the mineral compositions given in Table 3-3.

3.5 Conclusions

El Teniente is a block cave mine in Chile and a significant producer of copper and molybdenum in the world. The majority of the mine’s current production comes from hard, competent primary ore situated under high in-situ stresses. Naturally, rock mass cavability, primary and secondary fragmentation, excavation stability, and mine-induced seismicity are the areas of high concern for the mine. CMET is one of the main lithological units hosting mineralization at El Teniente. In-situ, it is sparsely jointed but contains a dense network of mineral filled veins - stockwork. When tested in compression, specimens of CMET tend to exhibit brittle behaviour.

CMET, also known as veined mine andesite, was selected for the laboratory experiments of this research. As it was demonstrated in this chapter, CMET satisfied the characteristics of the target rock type, was not studied experimentally in detail before, and the resulting work could directly provide benefits to the mining operation. These were the primary reasons for selecting CMET for the role of experimental material.

The specimens for the laboratory experiments were cored out of two 150 mm diameter core sections provided by the mine. The large-diameter cores were drilled on the Teniente 7 level, approximately 122 m below the undercut level of the Esmeralda mine sector of El Teniente. Ten 125 mm long 50 mm in diameter test specimens were prepared at the University of Toronto in
accordance with the recommendations of ISRM for testing of intact rock in triaxial compression (ISRM 1978). Each specimen undergone extensive characterization, including mineralogical composition. Petrographic analyses were done using thin sections, which were prepared after the completion of the experiments.

Presence of veins was a major characteristics of the tested specimens. Comprehensive vein characterization was a complex task to which a separate thesis chapter is dedicated. The next chapter presents details of vein characteristics of the specimens of intact veined rock, including vein geometries, mineralogy, and possible correlations between the two categories.
4.1 Introduction

This chapter presents the results of characterization of vein geometry and mineralogy for the core specimens of intact veined rock. The objective of the vein characterization was to develop understanding of vein parameters that may influence the results of the compression experiments. Geometrical characterization involved construction of 3D discrete vein networks (DVN). In order to accomplish this, a unique approach was developed allowing to construct a DVN from photographs of the specimen. The approach is discussed in detail at the beginning of the chapter. Data on vein thickness and vein angles within the specimen are presented for mapped veins. Detailed description of vein mineralogical composition and correlations between vein mineralogy and vein geometry are presented at the end of the chapter.

4.2 Discrete Vein Networks

In order to refer to a geometric arrangement of veins within a specimen, a term *discrete vein network* is introduced. A discrete vein network (DVN) is a 3D geometrical representation of mineral-filled veins that are present within the volume of a specimen. A DVN was built for each intact veined specimen selected for laboratory testing using CAD tools. The vein networks served two purposes. Initially, they were used to help with the interpretation of the experimental results. DVNs were also utilized in construction of numerical models for the tested specimens.

4.2.1 Construction Methodology

Each discrete vein network was built based on surface exposures of the veins in a specimen. A unique approach, which is illustrated in Figure 4-1, was developed to accomplish the DVN construction.

High-resolution digital photographs of the specimen that were taken as part of the preparation process were used to construct a composite image of the specimen sidewall. The composite was assembled from 12 photographs, each representing a 30° sector of the specimen’s circumference. Each image was properly scaled and corrected for distortion based on the grid that was drawn on the specimen sidewall. Individual images were arranged into a mosaic which
represented an image of an ‘unrolled’ sidewall of the specimen (Figure 4-1a). Photographs of the top and bottom specimen surfaces were also introduced.

Figure 4-1. Illustration of the methodology developed for discrete vein network construction for intact veined rock specimens.

In the second step, visible vein exposures were traced in two-dimensional space using the mosaic image. This task was carried out using AutoCAD software. To ensure continuity of vein traces, it was critical for the vein traces to terminate at matching points on the opposite sides of the image. Vein offsets that were visible in the composite image were properly captured by the tracing process. If a vein was offset by another, the offset vein was represented by two or more traces. Every vein was represented by a single trace, independent of vein thickness. The original specimen was consulted at all times during the tracing process to ensure DVN accuracy. Figure 4-1a illustrates the result of the vein tracing step.

Once the veins were traced, the resulting 2D geometry was draped onto a cylinder using a 3D solid-modelling software. Rhonoceros3D package was used for this task. The composite image of the specimen sidewall and images of the top and bottom surfaces were also added. Vein traces
were then finalized; trace segments were added for veins that intersected specimen ends. Figure 4-1b illustrates the result of this step.

In the final stage of the DVN construction, a three-dimensional representation of the vein network was built. For each vein trace, a triangulated mesh was constructed. For veins that intersected the specimen completely, the vein outline was fitted with a patch. For veins that terminated on other veins inside the specimen, a plane was first fitted through the points describing the veins geometry on surface. The resulting plane was then extended to intersect the terminating veins. The resulting lines of intersection were added to the original surface trace of the vein being constructed, and the triangulated mesh patch was then built. Overall, construction of 3D vein geometry honored all interpreted vein terminations. The result of the vein construction process was a 3D representation of the vein network (Figure 4-1c).

4.2.2 Vein Geometry

The results of the DVN construction work are shown in Figure 4-2. Specimen 1 contained the highest number (23) of mapped vein segments. Specimen 14 contained 6 vein segments, the lowest of the ten specimens. On average, a specimen contained 15 vein segments. Each vein segment was individually numbered for identification. A catalogue of specimen veins was created.

Figure 4-2. Illustrations of specimens and constructed discrete vein networks for the specimens selected for laboratory testing.
4.2.3 Discussion

The process of DVN construction demonstrated the complexity and variability of vein geometry. Even on the scale of 50 mm diameter core, the specimens contained veins that traversed the specimen completely, terminated on other veins, and were offset by them (Figure 4-3). Vein geometric characteristics varied between the specimens in thickness and definition. For example, Specimens 1 and 2 were cored next to each other from the same large-diameter core piece, yet their vein geometries were different (Figure 4-4). Veins within Specimen 1 were thinner on average and geometrically better-defined. The veins of Specimen 2, on the other hand, were thicker and more difficult to interpret as discrete geometrical surfaces within the given specimen size. Specimen 14, also shown in Figure 4-4, was the most complex of the specimens in terms of interpretation of vein geometry. One can see that vein material occupied greater volume of the specimen than the host material. Interpretation of vein geometry was difficult in this specimen, and this is the reason why Specimen 14 contained the least number of veins (6) out of all the specimens of intact veined rock selected for testing.

Figure 4-3. Photographs of Specimens 7 and 19 illustrating offsetting veins, veins terminating in veins, and cross-cutting veins.
As described by the methodology (Section 4.2.1), the DVN construction process was based on vein surface exposures. Internally, vein geometries were extrapolated from surface traces. For planar veins, it is believed that this approach produced accurate vein surfaces. For veins that were kinked or terminated inside the specimen, the results were subject to interpretation.

The DVN geometries did not account for vein thickness. Independent of its thickness, a vein was represented by a single two-dimensional triangulated surface. This can be considered a limitation.

The vein network construction focused on capturing the geometry and position of well-exposed and adequately-represented veins. *Well-exposed* means that vein material was visible and easily-identifiable by a naked eye. *Adequately-represented* means that vein geometry could be represented by a single plane-like geometry. Veins in most of the specimens conformed well to these characteristics. However, veins in few specimens, for example Specimens 14 and 15, while
being well-exposed, could not be represented by a discrete vein, and consequently, were omitted from the vein network. This is a limitation of the DVN construction approach used in this work.

It is inevitable that the process also omitted veins that were either not exposed on the specimen’s surface or were unidentifiable on photographs and by naked eye. Again, this is one of the limitations of the vein identification method used in this work.

4.3 Vein Thickness

Thickness of each catalogued vein was measured from photographs. Depending on the length of the vein exposure and its visibility, the measurements of vein thickness were performed at a minimum of 2 locations, and for most veins 5 measurements were carried out. Table 4-1 summarizes vein thickness data.

Table 4-1. Summary of measured vein thicknesses in 10 specimens selected for laboratory testing.

<table>
<thead>
<tr>
<th>Vein ID</th>
<th>Average Vein Thickness (mm)</th>
<th>All</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>0.9</td>
<td>1.1</td>
</tr>
<tr>
<td>2</td>
<td>0.9</td>
<td>1.2</td>
</tr>
<tr>
<td>3</td>
<td>1.8</td>
<td>0.6</td>
</tr>
<tr>
<td>4</td>
<td>1.9</td>
<td>1.1</td>
</tr>
<tr>
<td>5</td>
<td>1.4</td>
<td>2.1</td>
</tr>
<tr>
<td>6</td>
<td>2.4</td>
<td>1.7</td>
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<td>7</td>
<td>0.3</td>
<td>1.7</td>
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<td>8</td>
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<td>2.9</td>
<td>0.7</td>
</tr>
<tr>
<td>10</td>
<td>0.4</td>
<td>0.6</td>
</tr>
<tr>
<td>11</td>
<td>0.6</td>
<td>0.7</td>
</tr>
<tr>
<td>12</td>
<td>0.3</td>
<td>0.6</td>
</tr>
<tr>
<td>13</td>
<td>0.3</td>
<td>0.7</td>
</tr>
<tr>
<td>14</td>
<td>0.8</td>
<td>0.5</td>
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<tr>
<td>15</td>
<td>0.7</td>
<td>0.6</td>
</tr>
<tr>
<td>16</td>
<td>0.9</td>
<td>0.7</td>
</tr>
<tr>
<td>17</td>
<td>0.4</td>
<td>0.5</td>
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<td>---------</td>
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<tr>
<td>18</td>
<td>0.4</td>
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<td>19</td>
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<td>0.4</td>
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</tr>
<tr>
<td>22</td>
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<td></td>
</tr>
<tr>
<td>23</td>
<td>0.3</td>
<td></td>
</tr>
<tr>
<td>Min.</td>
<td>0.3</td>
<td>0.5</td>
</tr>
<tr>
<td>Max.</td>
<td>2.9</td>
<td>4.7</td>
</tr>
<tr>
<td>Avrg.</td>
<td>0.9</td>
<td>1.2</td>
</tr>
</tbody>
</table>

It was discussed in Chapter 2 that the cumulative distribution of vein thicknesses within a population normally follows a power-law distribution. Figure 4-5 shows a plot of cumulative vein frequency values from Table 4-1 plotted in log-log space.

![Figure 4-5. Power-law distribution of vein thickness values from the intact veined rock specimens.](image_url)
From Figure 4-5 one can see that the vein thickness data appear to follow two distributions. Cumulative frequency of veins having thickness below 1.5 mm follows the distribution approximated by the black line (Figure 4-5), with the scaling factor \( D \) of 0.7. The distribution of veins having thickness above 1.5 mm fits the distribution indicated by the red line (Figure 4-5), with the scaling factor of 2.1.

The distribution for the veins thinner than 1.5 mm fits with the observations of Gillespie et al. (1999) and Loriga (1999) who reported scaling factors for vein thickness distributions to be in the 0.6-0.9 range. Out of the two distributions, the one with \( D=0.7 \) is likely to be representative of the veins of CMET at El Teniente. This distribution does not fit the veins with thickness above 1.5 mm. This is because the specimens cover limited extent within the rock mass, and, naturally, occurrence of thicker veins has not been captured adequately. André-Mayer and Sausse (2007) showed that the relationship between vein thickness and vein spacing followed a power-law relationship. This means that the spacing between thicker veins tends to be larger than between thinner ones. In the case of our specimens, it is natural that the 125 mm long specimens likely captured enough number of veins that were less than 1.5 mm thick and very limited number of ones that had a thickness above 1.5 mm.

### 4.4 Vein Angle

Angle with respect to the diametric axis of the specimen was measured for each catalogued vein. A plane was fitted through the points that defined the vein’s surface and a dip value of this plane with respect to the short axis of the specimen was established, defining the vein angle. Measured vein angles are summarized in Table 4-2.

The objectives of the vein angle analysis was to investigate if the veins in the selected specimens had preferred orientations with respect to the short axis of the specimen and whether the specimens contained veins that were oriented favourably for fracture propagation in triaxial tests. Histograms on measured vein angles (Table 4-2) for the specimens selected for testing are shown in Figure 4-6.
Table 4-2. Summary of measured vein angles 10 specimens selected for laboratory testing.
The angle is with respect to the specimen’s diametric axis.

<table>
<thead>
<tr>
<th>Vein ID</th>
<th>Vein Angle wrt Diametric Axis of Specimen (degrees)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>65</td>
</tr>
<tr>
<td>2</td>
<td>67</td>
</tr>
<tr>
<td>3</td>
<td>62</td>
</tr>
<tr>
<td>4</td>
<td>31</td>
</tr>
<tr>
<td>5</td>
<td>17</td>
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<tr>
<td>6</td>
<td>33</td>
</tr>
<tr>
<td>7</td>
<td>14</td>
</tr>
<tr>
<td>8</td>
<td>45</td>
</tr>
<tr>
<td>9</td>
<td>45</td>
</tr>
<tr>
<td>10</td>
<td>62</td>
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<tr>
<td>11</td>
<td>73</td>
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<tr>
<td>22</td>
<td></td>
</tr>
<tr>
<td>23</td>
<td></td>
</tr>
</tbody>
</table>

From Figure 4-6 one can see that the majority of the specimens contained veins oriented at various angles to the diametric axes, covering the full range of angles, from being sub-parallel to sub-normal to diametric axes. In Specimens 5, 6, 8, 12, and 19, the distributions of vein angles were close to being uniform. Specimen 1 contained 15 veins (65% of the mapped veins) with the angle of less than 45° and 5 veins with the angle between 60° and 75°. The majority of the veins (14 out of 17) in Specimen 2 had an angle of greater than 45° and 8 veins (nearly half) fell in the...
60-75° range. Specimen 7 contained 8 (out of 21) veins having angles between 60° and 75°. Only 6 veins were mapped in Specimen 14, but all of them had an angle of greater than 45°. Eleven out of 17 veins of Specimen 15 had an angle between 45° and 75°.

In triaxial compression experiments on cylinders of intact rock, rupture planes tend to be oriented at angles between 50° and 70° to the diametric axis of the specimens (Paterson and Wong 2005). In a specimen of intact veined rock, presence of veins that are inclined at 50-70° to the short axis of the cylinder may result in the specimen being preconditioned to fracturing, provided that the vein strength is lower than the strength of the host material. Veins oriented within this range of angles were present in all selected specimens of intact veined rock, and a number of specimens contained many of such veins.

Figure 4-6. Distributions of angles with respect to the diametric axis of the specimen for mapped veins in the specimens selected for triaxial testing.
4.5 Mineralogy

After the ten specimens of the intact veined rock were tested in the laboratory, fifty two thin section were prepared from disks cut parallel to the diametric axes of the specimens. Petrographic analysis (O'Shaughnessy and Mailloux 2015) was performed for each thin section to establish mineralogical compositions of the specimens’ host material (results were discussed in Chapter 3) and of mapped veins. Figure 4-7 illustrates vein identification that was carried out. Appendix B provides a detailed account of vein mineralogy.

Figure 4-7. Illustration of a thin section in cross polarised (left) and plane polarised light (right) with identified veins marked by red lines and a fracture marked by blue line (O'Shaughnessy and Mailloux 2015).

The interest in the mineralogy of the veins was two-fold. First, the mineral composition of the veins that were involved in specimen fracturing was used to establish any potential correlations between fracturing and vein mineralogy. Second, vein mineralogical composition was used to investigate if vein thickness and vein angles were related to the presence of any particular minerals.

The assessment of vein mineralogy showed that quartz and anhydrite were the two most dominant minerals, occurring within almost every vein of the tested specimens. Their volumetric content ranged between 3% and 95%. Other minerals that were found to occur frequently included plagioclase, biotite, chlorite, and chalcopyrite.
No obvious relationships were detected to exist between vein mineralogy and vein thickness. Figure 4-8 illustrates plots of vein thickness data vs. volumetric content of major minerals for all tested intact veined rock specimens. Similar conclusions were made by Brzovic (2010).

Similarly, the analyses showed that no relationship was evident between vein mineralogy and vein angle. Figure 4-9 and Figure 4-10 illustrate plots of vein mineral content vs. vein angle for specimens drilled from the large-diameter core S01 (Specimens 1-8) and S02 (Specimens 12-19), respectively.

Figure 4-8. Plots of vein thickness and volumetric content of main minerals for all tested intact vein specimens.
Figure 4-9. Plots of vein angle with respect to the short axis of the specimen and volumetric content of main minerals for Specimens 1-8 (large diameter core S01).
4.6 Conclusions

The aim of this chapter was to develop an understanding of the vein characteristics of the intact veined rock specimens selected for triaxial compression testing. This was accomplished through vein characterization based on vein geometry, thickness, and mineralogical composition.

Geometrical characterisation included cataloguing of veins and construction of a discrete vein network for each specimen. A methodology for vein mapping and DVN construction was developed. The DVN construction exercise showed that each of the specimens contained between 6 and 23 veins.

Measurements of average thickness and angle with respect to the diametric axis of the specimen were carried out for each catalogued vein. The analysis demonstrated that the measured
vein thicknesses followed two power-law distributions. The distribution of veins having thickness less than 1.5 mm was characterized by the scaling factors of 0.7. The distribution of veins having thickness greater than 1.5 mm was characterized by a scaling factors of 2.1. A scaling factor in the range between 0.6 and 0.9 is often observed in vein thickness distributions (Gillespie et al. 1999; Loriga 1999).

Analyses of vein inclination data demonstrated that the veins in the specimens were oriented at variety of angles, covering a range between 0° and 90°. In many specimens the distributions of angles for the catalogued veins were nearly uniform. All specimens contained veins inclined 50-70° to the diametric axes of the specimen, being preferentially-oriented for the development of rupture in triaxial compression experiments.

Mineralogical analysis of vein infilling material was performed by petrographic analysis of thin sections. The analyses demonstrated that anhydrite and quartz were most dominant infill minerals in the specimen veins. Plagioclase, biotite, chlorite, and chalcopyrite were also found to be present.

As minerals differ in hardness, variations in vein mineral composition affect vein strength. This information is taken into account when the results of triaxial tests are presented and discussed in Chapter 6 and Chapter 7. There it is shown how vein mineral composition correlates with specimen strengths.

In Chapter 5, the experimental approach and the setup used in the triaxial experiments are detailed. The chapter initially provides a description of how intact rock behaves in compression during laboratory experiments, aiming to define a framework within which experimental results on intact veined rock can be presented later on.
Chapter 5
Review of Brittle Failure of Intact Rock and Setup of the Triaxial Experiments

5.1 Introduction

The aim of this chapter is to describe the experimental approach and laboratory setup that were used in carrying of the triaxial testing of intact veined rock specimens. The chapter begins with a summary of the current understanding of the behaviour and progressive failure of intact rock and intact andesite in laboratory experiments. Understanding of how intact rock behaves in compression will help to understand the results of the experiments on intact veined rock, which are presented in Chapter 6 and Chapter 7.

Laboratory compression experiments have been traditionally used to study rock behaviour under stress (e.g. Bieniawski 1967; Brace et al. 1966; Brace 1978; Eberhardt et al. 1998; Martin and Chandler 1994; Scholz 1968). Because field-scale or in-situ experiments are expensive, poorly-controlled, and often unpredictable, laboratory experimentation is best for in-depth studies of rock behaviour under stress. Laboratory experiments can be carefully designed and executed under controlled conditions.

In this research, ten experiments on the intact veined specimens were conducted. In five triaxial experiments, the loading followed a stress path that was similar to what rock would experience with an approaching caving front. Another five experiments were standard triaxial tests with a range of confining pressures.

Specimens of intact veined rock are heterogeneous. Studies on gneiss, schist, and slate (e.g. Ghazvinian et al. 2015; Rawling et al. 2002; Slatalla and Alber 2014; Zhang et al. 2011) show that the presence of directional features affects the behaviour of intact rock. The behaviour of these rock types, however, is bound to differ from veined rock because the nature of the heterogeneity. In gneiss, schist, and slate, the fabric is pervasive throughout the entire specimen, with constant orientation. In specimens of intact veined rock, veins are discrete features that have various orientations. This is one of key differences of veined rock from heterogeneous rocks characterized by the presence of fabric.
5.2 Experimental Theory of Brittle Failure of Intact Rock

Brittle fracture of intact rock in uniaxial and triaxial (with equal intermediate and minor compressive stresses) compression on laboratory-sale specimens has been studied in detail experimentally by many researchers during the past few decades (e.g. Bieniawski 1967; Brace et al. 1966; Martin and Chandler 1994; Nicksiar and Martin 2014; Scholz 1968). As the result, the rock mechanics community has been able to develop fundamental understanding of the progressive failure of intact rock in compression, including different stages involved, parameters affecting the process, and how these can be used in engineering design applications. In order to discuss the results of the experiments conducted on intact veined rock, we briefly review some of the background theory of brittle fracture process in intact low-porosity crystalline rock, specifically from the experimental perspective. This is necessary for a subsequent discussion of differences that were observed in our experiments.

Figure 5-1 shows idealized experimental results from an unconfined compression test on intact rock. Redrawn largely based on work of Martin (1993), who extensively studied the behaviour of Lac du Bonnet granite, the figure is well-suited to illustrate different stages of the progressive failure of an intact rock specimen in compression.

The process of progressive failure can be subdivided into 5 stages (Martin and Chandler 1994). During Stage I, a rock specimen normally exhibits nonlinear, concave stress-strain response when subjected to compression (Figure 5-1). This is commonly referred as the crack closure stage (Martin and Chandler 1994). As the name suggests, existing cracks close and pores between grains collapse during this stage. The extent of Stage I depends on the initial porosity of the specimen and may be absent altogether. The crack closure stress threshold, $\sigma_{cc}$, marks the end of Stage I. Paterson and Wong (2005) show that P-wave velocities ($V_p$) in both axial and lateral direction tend to increase while crack closure takes place.

The elastic stage (Stage II) follows the crack closure (Martin and Chandler 1994). A rock specimen exhibits a linear stress-strain response. The elastic properties, Young’s modulus and Poisson’s ratio, are determined based on the stress-strain changes observed at this stage of an experiment.
Figure 5-1. Stages in the progressive failure of intact rock in unconfined compression (after Martin 1993; Paterson and Wong 2005).

The onset of dilatancy – increase in volume relative to elastic changes (Brace et al. 1966) – indicates the beginning of Stage III, the stage during which stable fracture development takes place. Experimental research by Brace et al. (1966) and Martin and Chandler (1994) showed that the fracture development in this stage is dominated by the formation of cracks oriented parallel to the direction of the applied principal stress, i.e. vertically. Experiments also show that if specimen loading is stopped during this stage, crack formation ceases. Upon unloading, axial strain recovers fully but lateral strain only in part (Brace et al. 1966). Martin and Chandler (1994) point out that the crack initiation stress threshold, $\sigma_{ci}$ (Figure 5-1), cannot be detected based on axial strain, and
that the deviation from linearity in the lateral strain is what indicates the beginning of Stage III. Similarly, Brace et al. (1966) used volumetric strain changes to detect specimen dilatation in unconfined and confined experiments.

Studies involving monitoring of acoustic emissions (AE) during experiments on rock have shown that onset of AE activity correlates with the beginning of the stable crack growth stage. Scholz (1968) used AE to identify the $C'$ threshold, the stress difference at the crack initiation point, in unconfined and confined experiments on various types of rock.

Formation of axial cracks during the stable crack growth stage is also supported by experimental studies involving measurements of ultrasonic wave velocities. Experiments by Hadley (1975) and Lockner et al. (1977) indicated that P-wave velocities in the direction perpendicular to the maximum applied stress tended to decrease significantly at the stress difference of approximately half of the stress difference at fracture. Similarly, experiments by Gupta (1973) showed pronounced decrease in $V_p$ values along lateral specimen axes and only slight decrease in the velocity along the axis of major stress.

The value of the crack initiation stress increases with application of confining pressure. Therefore, it is convenient to express it as a fraction of the stress difference at fracture ($C = \sigma_f - p_c$, where $p_c$ is the confining pressure), or $C'/C$. Martin and Chandler (1994) reported $C'/C$ ratio for Lac du Bonnet granite being approximately 0.4 in unconfined experiments. Brace et al. (1966) reported $C'/C$ ratio to range between 0.3 and 0.6 for Westerly granite, 0.4 to 0.7 for aplite, and 0.45 to 0.65 for marble based on unconfined and confined experiments. Conducting unconfined and confined tests on various crystalline rocks, Scholz (1968) established a range of $C'/C$ values to be between 0.3 and 0.6, concluding that “for a very wide variety of brittle rocks of diverse structure and composition and over a wide range of confining pressure, deformation is accompanied by small-scale fracturing that begins at about half the fracture stress.”

The stable crack growth stage is followed by unstable cracking (Stage IV). Even though tensile fractures continue to develop during this phase, sliding at flaws and grain boundaries increasingly becomes the dominant mechanism of the micro-failure (Hallbauer et al. 1973; Martin and Chandler 1994). Confining pressure affects the development of unstable fracture propagation. Bieniawski (1967) noted that the unstable cracking may not develop under high confining pressure.
Bieniawski (1967; 1968) and Martin and Chandler (1994) defined the onset of unstable cracking by the point of the volumetric strain reversal as illustrated in Figure 5-1. The axial crack damage stress threshold \( \sigma_{cd} \) nominally marks the transition from Stage III to IV.

Martin and Chandler (1994) reported that once unstable cracking of a specimen commenced, it could not be stopped even if further specimen loading was ceased. Lac du Bonnet granite specimens failed almost immediately once axial loads in unconfined tests exceeded the \( \sigma_{cd} \) threshold (Martin and Chandler 1994). Bieniawski (1967) reported that once unstable fracture propagation commenced, the final mode of specimen fracturing was governed by the type of loading and platens used in the experiment. In unconfined experiments, platen size and stiffness of the loading rig governed whether conical, shearing or slabbing of the specimen resulted at failure. In confined experiments, it was at the unstable cracking stage that the confining pressure influenced the formation of the final shear fracture. Bieniawski (1967) pointed out that these phenomena were not observed while specimen fracturing was stable.

Experiments by Martin (1993) showed that AE activity increased dramatically once unstable cracking began. Also, axial P-wave velocities tend to decrease as more and more shear-type fractures are developed.

Martin and Chandler (1994) reported that \( \sigma_{cd} \) normally ranged between 0.7 and 0.85 of the unconfined compressive strength. Xue et al. (2014) in their analyses of published data on the crack damage threshold of various rock types established that the \( \sigma_{cd}/\sigma_f \) ratio for igneous and metamorphic rocks varies between 0.6 and 0.9.

The post-peak stage (Stage V) commences once the peak stress is reached. The unstable cracking phase eventually results in localization of microcracks into a macroscopic fracture (Paterson and Wong 2005). The peak axial stress, \( \sigma_f \), attained by a specimen during an experiment marks the specimen’s peak strength (Figure 5-1). It has been shown (Hudson et al. 1972) that the specimen’s peak strength is greatly influenced by the boundary conditions and, therefore, is not an inherent property of the material. Confining pressure influences the type of response the specimen shows in the post-peak. In unconfined experiments, axial stress decreases rapidly with strain. In experiments with high confining pressure, ductile behaviour may be observed.
5.3 Experimental Studies on Intact Andesites

Experimental studies on intact (non-veined) andesite show that the rock tends to display the general characteristics outlined in Section 5.2. These include levels of the $\sigma_{ci}$ and $\sigma_{cd}$ thresholds with respect to peak strengths, correlation between the onset of dilatancy and the onset of AE activity, and evolution of axial and diametric P-wave velocities. All five stages of progressive failure are normally identified during uniaxial and triaxial tests.

Experiments of Siratovich et al. (2014) on Rotokawa Andesite (New Zealand) demonstrated variable specimen strengths (60-211 MPa), which showed negative correlation with porosity due to the presence of microcracks. Calculated values of elastic parameters ranged between 20 and 44 GPa (mean of 31 GPa) for the Young’s modulus and 0.09 and 0.34 (mean of 0.20) for the Poisson’s ratio. The $C'/C$ values ranged between 0.36 and 0.47 which are common for intact rock. Rapid increase of AE activity was recorded at the onset of specimen dilatancy.

Laboratory experiments of Heap et al. (2014) on andesite from Volcán de Colima (Mexico) showed significant variations in peak strength (24-124 MPa), which were attributed to different degrees of specimen microcracking. Similar to Rotokawa Andesite (Siratovich et al. 2014), the strength showed negative correlation with porosity. Young’s moduli varied between 9 GPa for highly-porous (over 20% total porosity) specimens and 36 GPa for less porous (14% total porosity) specimens. The $\sigma_{ci}$ thresholds were identified to vary between 0.13 and 0.43 of the peak strengths. Increase in AE activity correlated with the onset of specimen dilatancy.

Rao and Kusunose (1995) conducted triaxial experiments on specimens of Yugawara Andesite (Japan) under the confining pressure of 40 MPa. In their experiments, the specimens reached the peak strength of 565 MPa. The specimen dilatancy was recorded at $C'/C$ values of 0.4, and the ratio of the stress difference of the crack damage threshold ($D = \sigma_{cd} - p_c$) to $C$, or $D/C$, was 0.7. The onset of AE activity was noted to correlate with the onset of dilatancy.

In Table 5-1, the aforementioned results of the experiments on the three types of intact andesite are summarized. These will be referenced when discussing the results of the experiments on intact veined andesite form El Teniente.
Table 5-1. Summary of key experimental results on 3 types of intact andesite (Heap et al. 2014; Rao and Kusunose 1995; Siratovich et al. 2014).

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Rotokawa Andesite</th>
<th>Volcán de Colima Andesite</th>
<th>Yugawara Andesite</th>
</tr>
</thead>
<tbody>
<tr>
<td>Test type</td>
<td>Unconfined</td>
<td>Unconfined</td>
<td>Triaxial (p_c = 40 MPa)</td>
</tr>
<tr>
<td>Young’s modulus, E (GPa)</td>
<td>20-44</td>
<td>9-36</td>
<td>-</td>
</tr>
<tr>
<td>Poisson’s ratio, ν</td>
<td>0.09-0.34</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Peak strength, $\sigma_f$ (MPa)</td>
<td>60-211</td>
<td>24-124</td>
<td>565</td>
</tr>
<tr>
<td>$C'/C = (\sigma_{ci} - p_c) / (\sigma_f - p_c)$</td>
<td>0.36-0.47</td>
<td>0.13-0.43</td>
<td>0.4</td>
</tr>
<tr>
<td>$D / C = (\sigma_{ci} - p_c) / (\sigma_f - p_c)$</td>
<td>-</td>
<td>-</td>
<td>0.7</td>
</tr>
</tbody>
</table>

As mineralogical composition of andesite varies depending on its origin, experimental results are also expected to vary among different andesites. However, as shown by the analyses of the three types of andesite (Table 5-1), generic trends in the experimental results, for example $C'/C$ ratio, should be fairly consistent.

5.4 Experimental Approach

Triaxial compression experiments were carried out at the Rock Fracture Laboratory (RFL), at the University of Toronto, Canada. Two sets of triaxial experiments, five tests each, were performed.

Initially, five experiments were conducted under identical loading conditions. The adopted loading procedure attempted to reproduce a stress path that was similar to what rock mass may experience during advance of a caving front. Section 5.5.3 describes the stress path.

Following the analyses of the results from the initial tests, the design of the experiments was adjusted. Greater emphasis was placed on the acquisition and processing of acoustic emission data. The specimens were loaded following a standard loading approach used in triaxial compressing experiments (see Section 5.5.3). Confining pressures between 2 and 60 MPa were used.
5.5 Experimental Setup

5.5.1 Testing Equipment

The testing was carried out using a specialized triaxial geophysical imaging cell that was developed by ErgoTech Ltd. (see Figure 5-2 and Figure 5-3). The axial compression load to the specimen was delivered using a floor-mounted computer-controlled MTS loading frame (Figure 5-2). Applied forces were measured by sensors built into the loading frame. The confining pressure was applied using silicon oil delivered by a manually-operated pump and monitored using a pressure sensor built into the testing cell.

Figure 5-2. MTS loading frame is shown in photograph a). Photograph of the geophysical imaging cell is shown in b). Geophysical imaging cell installed within the loading frame is shown in photograph c).
Axial deformations of the specimen were monitored with a pair of linear variable displacement transducers (LVDT) mounted between the arms, attached to the loading platens, outside the cell (Figure 5-3). Strain gauges attached to a pair of orthogonal cantilevers, housed inside the cell, measured diametric strain mid-height of the specimen in two directions (Figure 5-3).

Figure 5-3. Illustration of the triaxial geophysical imaging cell used in the experiments (after Goodfellow 2015).

Readings from the force sensors, LVDTs, and strain gauges were monitored and recorded during the experiment using a computer-based system developed in the LabView software. Five readings of each parameter were recorded per second. Figure 5-4 shows a photograph of the control station used to run the experiments.
The imaging cell was equipped with 18 ErgoTech MkII piezoelectric sensors allowing for monitoring of acoustic emission (AE) activity during the test. Each loading platen housed 3 sensors, and the remaining 12 sensors were embedded in the rubber jacket, surrounding the specimen (see Figure 5-3). The sensors had broadband velocity sensitivity between 10 kHz and 2 MHz, with roughly flat velocity sensitivity between 100 kHz and 1 MHz with no significant azimuthal dependency (Goodfellow 2015). The cell also contained 3 pairs of ultrasonic wave transducers for measurements of P-waves along 3 orthogonal axes of the specimen. These measurements were triggered manually and could be carried out at any time during the experiment.

Figure 5-4. Photograph of the station used to control the experiments.

The laboratory setup for the acoustic emission data acquisition is shown in Figure 5-5. Signals from the piezo-electric sensors were amplified using pre-amplifiers developed by Applied Seismology Consultants (ASC). Each channel’s amplified signal was split, feeding two independent data processing systems. The trigger system (Milne) operated based on a common AE data acquisition methodology. An AE event was registered when the amplitude of the incoming
signal exceeded a threshold value on a specified number of channels. This system was used continuously through the test. The second system (Richter) continuously recorded the amplified signals from the piezo-electric sensors. ASC’s 12-bit resolution recorders were used, sampling the signals at 10 MHz frequency. Due to the large amount of data generated by the Richter system, continuous data recorders were activated only after AE events began being registered by the trigger-based system. Both the trigger-based and the continuous data acquisition systems were managed using the InSite software (ASC 2014).

Figure 5-5. Schematic of the acoustic emission (AE) monitoring system used during the experiments (after Goodfellow 2015).

5.5.2 Testing Procedure

The execution of an experiment began with loading of a test specimen into the cell, inside the rubber jacket. The orientation of the specimen with respect to the orientation of the piezo-
electric sensors was recorded. Once the specimen was set within the cell, the cell was placed into the MTS loading frame.

The axial loading of the specimen was carried out under displacement control, in constant strain mode. The rate of loading of 0.0002 mm/s (strain rate of 1.6e-6 s\(^{-1}\)) was used. In parallel, the confining pressure was applied. Application of confining pressure differed between the two sets of experiments (see Section 5.5.3). Specimen loading continued until axial load stabilized following the failure of the specimen. Axial loading was stopped once the experiment was complete.

Experimental parameters were recorded with a frequency of 5 measurements per second. The data included axial deformation, radial deformation, axial force, and confining pressure. Ultrasonic velocity surveys were carried out throughout the experiment. These were initiated manually, approximately once per every 5-10 MPa of axial stress increase.

### 5.5.3 Specimen Loading

#### 5.5.3.1 “Caving” Stress Path Triaxial Tests

The specimen loading in the first five tests attempted to reproduce a stress path similar to the one recorded during the propagation of a cave front at the Esmeralda sector of El Teniente. Brzovic (2010) reported on a series of stress measurements that were carried out over a four-year period. The measurements aimed to capture a redistribution of stresses during the propagation of a caving front. The stresses were measured on the extraction level of the Esmeralda sector. Figure 5-6 shows the measured stresses as well as a simplified stress path adopted for the experiments.

In each experiment, the specimen was loaded uniformly until an axial pressure of 20 MPa was reached. Axial loading then proceeded using the specified loading rate while confining pressure was kept constant until an axial load of 42 MPa was reached. Subsequent loading proceeded in accordance with the selected stress path (see Figure 5-6). After the specimen reached an axial load of 75 MPa, the confining pressure remained constant at 12 MPa for the rest of the experiment.

The stress path in the experiments followed the major and minor principal stress magnitudes, ignoring the influence of the intermediate principal stress present in-situ. Standard triaxial experiments on cylindrical specimens cannot capture the behaviour of the intermediate
principal stress. Furthermore, major principal stresses also rotated as caving advanced. The laboratory experiments did not account for stress rotation that may occur in a mining context.

![Stress Diagram](image)

**Figure 5-6. Loading conditions used in the laboratory experiments based on the “caving” stress path loading.**

### 5.5.3.2 Triaxial Testing with Various Confining Pressure

In standard triaxial experiments, the specimen loading was carried out in accordance with the guidelines for triaxial testing as provided by ISRM (1983). The confining pressure of 2, 5, 30, 45, and 60 MPa was used. The axial loading of the specimen was carried out under displacement control. In parallel, the confining pressure was increased, such that the specimen was being loaded uniformly. Once the target confining pressure was reached, it remained constant until the end of the experiment, and the application of the axial loading continued with the target rate. Axial loading was stopped once the experiment was complete.
5.5.4 Data Processing

5.5.4.1 Stress/Strain Data

Axial stress, axial and lateral strains, volumetric strain, and elastic parameters, average axial Young’s modulus and Poisson’s ratio, were calculated according to standard methods (Fairhurst and Hudson 1999; ISRM 1983). The peak specimen strength was taken as the maximum axial pressure attained during the experiment. The volumetric strain ($\Delta V/V$ or $\varepsilon_V$) was calculated based on the relationship presented by Fairhurst and Hudson (1999):

$$\varepsilon_V = \varepsilon_a + 2\varepsilon_d$$  \hspace{1cm} (Eq. 5-1)

where $\varepsilon_a$ and $\varepsilon_d$ are measured axial and diametric strains respectively.

The elastic portion of the volumetric strain ($\varepsilon_{V\text{elastic}}$) was calculated based on the relationship of Martin (1993) and Martin and Chandler (1994):

$$\varepsilon_{V\text{elastic}} = \frac{1 - 2\nu}{E} (\sigma_1 - \sigma_3)$$  \hspace{1cm} (Eq. 5-2)

where $\nu$ and $E$ represent calculated values of Poisson’s ratio and of average Young’s modulus, and $\sigma_1$ and $\sigma_3$ are axial stress and confining pressure, respectively.

The portion of the volumetric strain that is attributed to the development of cracks, crack volumetric strain ($\varepsilon_{V\text{crack}}$), was calculated as a difference between the calculated volumetric strain and the calculated elastic strain as used by Martin (1993) and Martin and Chandler (1994):

$$\varepsilon_{V\text{crack}} = \varepsilon_V - \varepsilon_{V\text{elastic}}$$  \hspace{1cm} (Eq. 5-3)

The values for the crack closure ($\sigma_{cc}$), crack initiation ($\sigma_{ci}$), and crack damage ($\sigma_{cd}$) stress thresholds were assessed based on the changes in the calculated crack volumetric and volumetric strains as described by Martin and Chandler (1994) and Eberhardt et al. (1998). Figure 5-1 illustrates how these threshold values can be identified.

5.5.4.2 Acoustic Emission Data

Processing of AE data involved harvesting of the recorded continuous waveforms to obtain discrete events. The advantage of using continuous waveforms is that they allow one to optimize the event picking algorithm, thresholds, and filtering (Goodfellow et al. 2013).
For the AE data collected during triaxial experiments, an event was triggered if the amplitude of the waveform exceeded 70 mV on four channels within a window of 480 samples (48 µs). Upon satisfying this condition, an event file was created capturing 1024 data points, 384 points (37.5%) before and 640 points (62.5%) after the first arrival. P-wave arrival picking was carried out automatically for all recorded events. Event hypocentre locations were determined using the downhill Simplex method (ASC 2014) and a time-varying transverse isotropic velocity model. The propagation velocity values for the velocity model were obtained based on the measured axial and diametric P-wave velocities from the velocity surveys carried out during the experiment as described in Section 5.5.2. ASC’s InSite software was used for all AE data leaching, handling, and processing.

5.6 Conclusions

Studying behaviour of rock using laboratory experiments has advantages, such as ability to characterise in detail the tested specimen and to control the design and execution of the experiment. Our current understanding of how intact rock behaves in compression is based on decades of experimental work. When discussing progressive failure of intact rock, five stages are often recognized (Martin and Chandler 1994):

- Stage I: crack closure,
- Stage II: elastic deformation,
- Stage III: stable crack growth,
- Stage IV: unstable cracking, and
- Stage V: post-peak.

Stress thresholds between Stages I and II (crack closure, $\sigma_{cc}$), Stages II and III (crack initiation, $\sigma_{ci}$), Stages III and IV (crack damage, $\sigma_{cd}$), and Stages IV and V (peak strength, $\sigma_f$) identify important milestones in the evolution of the specimen state during compression.

In this research, two sets of experiments on intact specimens of veined andesite were carried out. The initial five experiments aimed to study veined rock behaviour under a stress path similar to what rock mass experiences during a propagation of a caving front. The remaining experiments used a loading approach that is used in standard triaxial experiments, focusing more
on acoustic emission response of the tested specimens. Experimental data were acquired using standard equipment and processed using standard, well-known industry techniques.

Experimental results are presented in detail in the next two chapters. Results of “caving” experiments are summarized in Chapter 6. Chapter 7 presents the results of tests with various levels of confining pressure.
Chapter 6
Influence of the “Caving” Stress Path on the Behaviour of Intact Veined Rock

6.1 Introduction

This chapter presents detailed results of the “caving” stress path experiments. Preliminary results were reported by Turichshev et al. (2012). Experimental results are summarised for each test individually. For each experiment, a figure is presented which combines stress-strain data, evolution of acoustic emission events, changes in axial and diametric P-wave velocities, changes in measured volumetric and calculated crack volumetric strain, and AE event locations. Based on the evolution of various data, interpretations of the crack closure, crack initiation, and crack damage stress thresholds are made following the approach presented in Chapter 5. Critical findings from the experiments are discussed at the end of the chapter.

The experiments included triaxial testing of Specimens 1, 2, 6, 14, and 15. The experiments were characterized by a stress path with varying confining pressure, aimed to mimic the effects of an approaching caving front in a block cave mine. The confining pressure reached a maximum of 28 MPa and then gradually decreased to 12 MPa, remaining constant at 12 MPa for the rest of the experiment. Details of the loading were given in Chapter 5.

Overall, the experiments were conducted as planned, and there were no major problems in their execution. A faulty circuit prevented correct measurements of lateral (diametric) strain during experiments 1 and 2 (Specimens 1 and 2). The problem was corrected, and a full suite of experimental results was collected in the remaining tests.

In describing specimen fracturing, veins that were involved in the disassembly of the specimen are referred using their number. The vein numbers for each specimen are as per the numbers found in Chapter 4.

6.2 Experiment 1, Specimen 1

Axial and diametric P-wave velocities were measured prior to specimen loading. The axial velocity was measured at 2,961 m/s and the diametric at 3,516 m/s, giving the ratio of the diametric to axial P-wave velocities of 1.19.
Figure 6-1 summarizes the experimental results of the test, including the stress-strain response and the AE event count in part a), evolution of the axial and diametric P-wave velocities in part b), and the interpreted AE event locations at various stages of the experiment in part c) of the figure. The specimen reached a peak compressive strength of 191 MPa. The average tangential Young’s modulus was 26.3 GPa. The Poisson’s ratio could not be established because lateral strain data were not collected due to a malfunctioning sensor.

The fracturing of the specimen occurred mostly along vein #3. The #3 vein had an average thickness of 1.82 mm and was oriented at 63°. The fracture also followed in part veins #10 (0.35 mm thick / 62°) and #11 (0.56 mm thick / 73°). The three veins were rich in quartz (>65%). The overall fracture orientation was 66° to the diametric axis of the specimen (Figure 6-2). The specimen exhibited brittle failure. The sudden loss of axial stress was followed by slight recovery. At the end of the experiment the axial load was recorded at 73 MPa.

The total number of AE events recorded during the experiment was 771. Out of the total events, locations were established for 314 (41% of the total number) events. Analysis of the acoustic emission data showed that only 7 events were recorded prior to the specimen reaching its peak strength (Figure 6-1a). The peak AE activity occurred between the time at which the peak strength was reached and the time of the abrupt stress drop (Figure 6-1a).

The interpretation of the AE event locations (Figure 6-1c) suggested that the fracture of the specimen was initiated at the specimen boundary, in the lower extremity of the fracture plane. The fracture then propagated towards the top end of the specimen.

Based on the evolution of the diametric P-wave velocity, it was estimated that the crack initiation stress threshold ($\sigma_{ci}$) was approximately at 133 MPa (Figure 6-1b). The ratio of the stress difference at the $\sigma_{ci}$ threshold to the stress difference at the peak stress ($C'/C$) was calculated to be 0.66.
Figure 6-1. Summary of experimental results from Specimen 1 test.

Specimen 1
Confining Stress: 12 MPa
Young’s Modulus, E: 26.3 GPa
Peak Strength: 191.0 MPa
Crack Initiation Stress, $\sigma_{ci}$: 133 MPa
6.3 Experiment 2, Specimen 2

Axial and diametric P-wave velocities for the specimen were measured prior to the experiment. The axial velocity was 3,262 m/s, and the diametric 4,105 m/s. The ratio of the diametric to axial P-wave velocities was 1.26.

The experimental results of the second experiment are summarized in Figure 6-3. The stress-strain response and the development of AE events are shown in part a). Part b) plots the evolution of P-wave velocities vs. axial strain. Interpreted locations of the AE events are visualized in part c) of the figure, which also shows locations of the fracture planes.

The specimen reached a peak compressive strength of 202.5 MPa. The average tangential Young’s modulus was 27.9 GPa. Fracturing of the specimen had two distinct stages. Upon the initial fracture, the stress dropped from its peak of 202.5 MPa to 181 MPa. A strength increase followed, with the axial stress reaching a new local maximum of 188 MPa. The second fracturing episode was a sudden brittle fracture of the specimen. The test was aborted shortly after the specimen rupture to avoid potential damage to the membrane housing the specimen. The final strength was recorded at 65 MPa.
Inspection of the specimen after the experiment revealed two sub-parallel fractures, oriented at 66° and 67° to the diametric axis of the specimen (Figure 6-4). Temporal analysis of the AE events suggested that the development of the lower fracture coincided with the first stress drop. The longer upper fracture occurred at a later time, coinciding with the second drop in axial stress.

The lower fracture propagated in part along veins #2 (1.16 mm thick / 75°) and #7 (1.66 mm thick / 69°). Both veins had similar mineralogical composition: ~40% quartz, 13-36% plagioclase, 14-23% anhydrite. The upper fracture propagated along vein #1. The #1 vein had an average thickness of 1.05 mm and was oriented at 66° with respect to the diametric axis of the
specimen. Its mineralogical composition differed from veins #2 and #7, containing 15% quartz and 85% anhydrite.

The total number of AE events recorded during the experiment was 1,200. Out of the total events, locations were established for 487 (41%) events. Similar to experiment 1, very few (10) AE events were recorded prior to the specimen reaching its peak strength (Figure 6-3a). The majority of the recorded AE events occurred in the lower part of the specimen, in the volume of the specimen bound by the two fractures.

Based on the evolution of the diametric P-wave velocity, it was estimated that the crack initiation stress threshold ($\sigma_{ci}$) was approximately 141 MPa (Figure 6-3b). The ratio of the stress difference at the $\sigma_{ci}$ threshold to the stress difference at peak ($C'/C$) was calculated to be 0.68.

Figure 6-4. Photographs of Specimen 2 after the experiment.

6.4 Experiment 3, Specimen 6

Axial and diametric P-wave velocities were measured prior to the experiment. The axial velocity was 3,825 m/s, and the diametric 4,031 m/s. The ratio of the diametric to axial P-wave velocities was 1.05.

The experimental results are summarized in Figure 6-5. The stress-strain response and the AE event development are shown in part a) of the figure. Diametric strain is plotted in part b).
Plots of the volumetric and crack volumetric strain are shown in part c) of the figure. Part d) displays the evolution of the axial and diametric P-wave velocities vs. axial strain. Interpreted locations of the AE events are visualized in part e).

A peak compressive strength of 179.6 MPa was reached in the experiment. The elastic constants, the average tangential Young’s modulus and the Poisson’s ratio, were 28.4 GPa and 0.3, respectively.

Figure 6-6 shows photographs of the fractured specimen. Specimen 6 fractured along vein #2 located in the lower part of the specimen. The #2 vein was 0.35 mm thick. Its mineralogy consisted of anhydrite (25%), quartz (70%), and biotite (5%). The fracture was oriented at 53° to the diametric axis of the specimen. The final strength of 84 MPa was recorded.

The total number of AE events recorded during the experiment was 661. Out of the total number, locations were established for 382 (58%) events. No AE events were recorded prior to the specimen reaching its peak strength (Figure 6-5a). The majority of the AE events were recorded during the fracturing of the specimen.

The crack closure stress threshold ($\sigma_{cc}$) was determined to be approximately 105 MPa. The $\sigma_{ci}$ and the $\sigma_{cd}$ thresholds were determined to be 139 and 178 MPa, respectively (Figure 6-3b). The $C'/C$ ratio was calculated being equal to 0.76. The ratio of the stress difference at the $\sigma_{cd}$ threshold to the stress difference at the peak stress ($D/C$) was calculated being equal to 0.99.
Figure 6-5. Summary of experimental results from Specimen 6 test.
Figure 6-6. Photographs of Specimen 6 after the experiment.

6.5 Experiment 4, Specimen 14

Axial and diametric P-wave velocities were measured prior to the commencement of the experiment. The axial velocity was 5,008 m/s, and the diametric 4,341 m/s. The ratio of the diametric to axial P-wave velocities was 0.87.

The results of the experiment on Specimen 14 are summarized in Figure 6-7. The stress-strain response and the development of AE events are shown in part a) of the figure. Diametric strain is plotted in part b). Plots of the volumetric and crack volumetric strain are shown in part c) of the figure. Part d) displays the evolution of the axial and diametric P-wave velocities. Interpreted locations of the AE events are visualized in part e).

A peak compressive strength of 142 MPa was reached in the experiment. The average tangential Young’s modulus and the Poisson’s ratio were 26 GPa and 0.28, respectively. A strength of 63.7 MPa was recorded at the end of the test.
Figure 6-7. Summary of experimental results from Specimen 14 test.

Specimen 14
- Confining Stress: 12 MPa
- Young’s Modulus, E: 26.0 GPa
- Poisson’s Ratio, ν: 0.28
- Peak Strength: 142.0 MPa
- Crack Closure Stress, $\sigma_{cc}$: 91 MPa
- Crack Initiation Stress, $\sigma_{ci}$: 110 MPa
- Crack Damage Stress, $\sigma_{cd}$: 139 MPa

AE Event Locations

I
II
III

66°

Alex Monet
Figure 6-8 shows photographs of the fractured specimen. In part, the fracture followed veins #1 (1.94 mm thick / 77°) and #3 (2.66 mm thick / 78°), but mostly through an undocumented vein. Veins #1 and #3 were mineralized. Vein #1 contained 25% anhydrite, 10% quartz, and 65% chalcopyrite. Vein #3 contained 25% anhydrite, 33% quartz, and 35% chalcopyrite. The overall orientation of the fracture plane was at 66° to the diametric axis of the specimen. The specimen appeared “bulked” after the test, possibly indicating existence of additional fractured regions that could not be identified based on surface examination.

The total number of AE events recorded during the experiment was 1780. Out of the total events, 369 (21%) were located. Contrary to the previous experiments and to the experiment on Specimen 15 (presented in Section 6.6), sustainable AE activity in experiment 4 commenced at the axial stress level that was much lower than the peak strength of the specimen (Figure 6-7a).

The crack closure stress threshold ($\sigma_{cc}$) was 91 MPa. The $\sigma_{ci}$ and the $\sigma_{cd}$ thresholds were 110 and 139 MPa, respectively (Figure 6-7b). The $C'/C$ ratio was calculated to be 0.75. The ratio of the stress difference at the $\sigma_{cd}$ threshold to the stress difference at peak was calculated being equal to 0.98.

Figure 6-8. Photographs of Specimen 14 after the experiment.
6.6 Experiment 5, Specimen 15

Axial and diametric P-wave velocities were measured prior to specimen loading. The axial velocity was 4,952 m/s, and the diametric 4,799 m/s. The ratio of the diametric to axial P-wave velocities was 0.97.

The experimental results are summarized in Figure 6-9. The stress-strain response and the AE event count are shown in part a) of the figure. Diametric strain is plotted in part b). Plots of the volumetric and crack volumetric strain are shown in part c) of the figure. Part d) displays the evolution of the axial and diametric P-wave velocities. Interpreted locations of the AE events are visualized in part e).

A peak compressive strength of 151.5 MPa was reached during the experiment. The average tangential Young’s modulus and the Poisson’s ratio were 31.6 GPa and 0.15, respectively. A final strength of 91.6 MPa was recorded at the end of the test.

Figure 6-10 shows photographs of the fractured specimen. The fracture followed vein #2 (0.39 mm thick / 50°). The vein was composed of 92% anhydrite and 8% quartz. Overall, the fracture was oriented at 53° to the diametric axis of the specimen. Specimen 15 exhibited the greatest amount of surficial damage out of all specimens tested.

The total number of AE events recorded during the experiment was 1,316. Out of the total events, locations were established for 712 (54%) events. No AE events were recorded prior to the specimen reaching its peak strength (Figure 6-9a). The majority of AE events were recorded during the fracturing of the specimen.

The crack closure stress threshold ($\sigma_{cc}$) was 92 MPa. The $\sigma_{ci}$ and the $\sigma_{cd}$ thresholds were 139 and 149 MPa, respectively (Figure 6-9b). The $C'/C$ ratio was 0.91. The ratio of the stress difference at the $\sigma_{cd}$ threshold to the stress difference at the peak stress was calculated to be 0.98.
Figure 6-9. Summary of experimental results from Specimen 15 test.

Specimen 15
Confining Stress: 12 MPa
Young’s Modulus, E: 31.6 GPa
Poisson’s Ratio, v: 0.15
Peak Strength: 151.5 MPa
Crack Closure Stress, $\sigma_{cc}$: 92 MPa
Crack Initiation Stress, $\sigma_{ci}$: 139 MPa
Crack Damage Stress, $\sigma_{cd}$: 149 MPa

AE Event Locations
I II III
6.7 Summary of Experimental Results

The key experimental results are summarized in Table 6-1. Each of the five tested specimens fractured along a discrete vein or a combination of veins. In two experiments (Specimens 6 and 15), the fractures were oriented at 53° to the diametric axis of the specimen. The fractures were oriented at 66° and 67° in the remaining experiments (Specimens 1, 2, and 14).

Table 6-1. Summary of test results from the experiments using “caving” stress path. The confining pressure ($p_c$) was taken as 12 MPa.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Sp.1</th>
<th>Sp.2</th>
<th>Sp.6</th>
<th>Sp.14</th>
<th>Sp.15</th>
</tr>
</thead>
<tbody>
<tr>
<td>Diametric P-wave velocity (m/s)</td>
<td>3,516</td>
<td>4,105</td>
<td>4,031</td>
<td>4,341</td>
<td>4,799</td>
</tr>
<tr>
<td>Axial P-wave velocity (m/s)</td>
<td>2,961</td>
<td>3,262</td>
<td>3,825</td>
<td>5,008</td>
<td>4,952</td>
</tr>
<tr>
<td>Diam. vel. / axial vel. ratio</td>
<td>1.19</td>
<td>1.26</td>
<td>1.05</td>
<td>0.87</td>
<td>0.97</td>
</tr>
<tr>
<td>Direction of regional $\sigma_1$ with respect to specimen long axis</td>
<td>(\perp)</td>
<td>(\perp)</td>
<td>(\perp)</td>
<td>(\parallel)</td>
<td>(\parallel)</td>
</tr>
<tr>
<td>Young’s modulus, E (GPa)</td>
<td>26.3</td>
<td>27.9</td>
<td>28.4</td>
<td>26.0</td>
<td>31.6</td>
</tr>
<tr>
<td>Poisson’s ratio, $\nu$</td>
<td>-</td>
<td>-</td>
<td>0.30</td>
<td>0.28</td>
<td>0.15</td>
</tr>
<tr>
<td>Bulk modulus, K (GPa)</td>
<td>-</td>
<td>-</td>
<td>23.7</td>
<td>19.7</td>
<td>15.0</td>
</tr>
<tr>
<td>Shear modulus, G (GPa)</td>
<td>-</td>
<td>-</td>
<td>10.9</td>
<td>10.2</td>
<td>13.7</td>
</tr>
<tr>
<td>Peak strength, $\sigma_f$ (MPa)</td>
<td>191.0</td>
<td>202.5</td>
<td>179.6</td>
<td>142.0</td>
<td>151.5</td>
</tr>
</tbody>
</table>
6.8 Discussion

6.8.1 P-Wave Velocities

In Specimens 1, 2, and 6, measured diametric P-wave velocities were higher than longitudinal velocities. This is indicative of the specimens containing pre-existing microcracks that were oriented normal to the long axes of the specimens. In-situ, these specimens were positioned with their long axes normal to the direction of the regional major principal stress ($\sigma_1$). Therefore, potential microcracks would be oriented sub-parallel to the direction of $\sigma_1$.

The opposite was observed in Specimens 14 and 15; measured diametric velocities in these specimens were lower than the axial velocities. This suggests that pre-existing microcracks were oriented parallel to the long axes of the specimens and, therefore, parallel to the direction of $\sigma_1$.

In laboratory experiments on intact rock, tensile micro-cracks develop parallel to the direction of loading (Wawersik and Fairhurst 1970). Micro-cracks tend to develop similarly in-situ. Plumb et al. (1984), for example, established such correlation at three different sites located in granites. The P-wave velocity measurements in the tested specimens support this idea.

The significance of this observation is that if a specimen is cored subparallel to the direction of $\sigma_1$, it is likely to contain microscopic flaws that are oriented with the coring direction. The preferentially-oriented flaws would be readily exploited during a compression experiment, leading to a lower compressive strength than if the specimen was cored at the same location but normal to the direction of the regional major principal stress.

6.8.2 Peak Strengths

The peak strengths attained in the experiments varied between 142 MPa and 202.5 MPa. The higher peak strengths, 191 MPa, 202.5 MPa, and 179.6 MPa, were observed in the tests on
Specimens 1, 2, and 6, respectively. One can see from Table 6-1 that these specimens were cored normal to the direction of $\sigma_1$. The peak strengths attained by Specimens 14 and 15 were 142 MPa and 151.5 MPa, respectively, the lowest of the tested specimens. Specimens 14 and 15 were cored sub-parallel to the direction of $\sigma_1$.

A linear correlation between the experimental peak strengths and the ratios of diametric to axial P-wave velocities (Figure 6-11) suggests that the peak strength was influenced by the orientation of the specimen in-situ with respect to $\sigma_1$. The ratio of the P-wave velocities can be viewed as a measure of influence of the specimen’s existing micro-defects on its strength in the direction of specimen loading.

![Figure 6-11](image)

**Figure 6-11. Linear correlation between the experimental peak strengths and the ratios of diametric to axial P-wave velocities for specimens of the experiments using the “caving” stress path.**

Presence of preferentially-oriented micro-defects with respect to the loading direction and their intensity, however, cannot be considered a determining factor in specimen strength. Because every specimen fractured along veins during the experiment, it is clear that veins had also affected
the strength. Brzovic (2010) suggested that veins with infilling that contained less than \( \frac{1}{3} \) of hard minerals actively promoted rock fracturing at El Teniente. Mineralogy and potentially other properties of veins in addition to micro-defect orientations influenced specimen strength in the experiments. This is discussed in Section 6.8.6.

### 6.8.3 Elastic Moduli

The test results showed that the moduli of elasticity of the specimens were between 26.3 and 31.6 GPa, with an average of 28.0 GPa. These values were noticeably lower than 50-60 GPa values normally reported for CMET (Table 3-1, Chapter 3) but comparable to the moduli of 35.2 ± 5.4 GPa (mean ± st. dev.) reported by Vallejos et al. (2016) for CMET and also to the values of intact andesite as was described in Section 5.3 of Chapter 5.

Exact reasons for why the tested specimens of intact veined rock demonstrated the lower Young’s moduli in comparison to the commonly referenced values are not known. The discrepancy can be explained by the presence of mineral veins, microcracks or both.

Experiments on anisotropic rocks, such as mica schist, slate, or foliated gneiss, demonstrate that the specimen Young’s modulus depends on the orientation of the weakness features (e.g. foliation) with respect to the loading direction (Rawling et al. 2002; Slatalla and Alber 2014). Specimen stiffness is reduced when these weakness features are oriented at an angle to the loading direction. When core is sampled for laboratory strength testing, it is common practice to select best core specimens, as homogenous as possible and free of visible defects and structural features. For the El Teniente’s CMET rock, the best specimens would be ones containing as few veins as possible, which are likely the type of specimens that are normally tested and whose values are likely to be published. In this work, the interest was in conducting experiments specifically on specimens containing many veins which are oriented at various angles. Naturally, such specimens will be less stiff than ones that had no or just a few veins.

Experiments of Siratovich et al. (2014) and Heap et al. (2014) on specimens of intact andesite showed that Young’s moduli were affected by the degree of specimen microcracking. The tested specimens of intact andesite, as described in Section 6.8.1, contained microcracks, which may explain relatively low values of Young’s modulus.
6.8.4 Dilatancy and Crack Initiation

The onset of dilatancy ($\sigma_{ci}$ threshold) is defined experimentally by the onset of inelastic lateral deformations of the specimen caused by the formation of vertical micro cracks (Brace et al. 1966). It is often accompanied by the onset of AE activity (Eberhardt et al. 1998; Scholz 1968).

In the tested specimens of intact veined rock, the $\sigma_{ci}$ values were consistent, ranging between 110 and 141 MPa, with an average of 132 MPa. The consistency in values agrees with the experimental results reported by Martin and Chandler (1994) who showed that the $\sigma_{ci}$ in Lac du Bonnet granite had small variation among the experiments and was independent of the specimen size. The values of the $C'/C$ ratios (parameters $C'$, $C$, and $D$ were explained in Chapter 5) in the experiments, however, were higher than anticipated, ranging between 0.66 and 0.91 (Table 6-1). By comparison, in experiments on intact rock, $C'/C$ ratios of 0.50 are normally observed (Brace et al. 1966; Scholz 1968). In experiments on intact andesite (Heap et al. 2014; Rao and Kusunose 1995; Siratovich et al. 2014), $C'/C$ value were reported to be below 0.5.

High $C'/C$ values in the tested intact veined specimens can be explained by either an elevated value of $C'$ or a reduced value of $C$. The experiments demonstrated that the crack initiation stress values were not influenced by variations in vein geometry, vein mineralogy, and other characteristics. At the same time, specimen peak strengths were influenced by veins (see Section 6.8.6). Relatively high values of $C'/C$ ratios were the result of lowered peak strengths.

6.8.5 Onset of Unstable Fracturing

The onset of unstable fracturing in the tested specimens of intact veined rock was high with respect to the peak strength ($D/C$ ratio of 0.98 and 0.99, Table 6-1) in comparison to the levels normally observed when testing specimens of intact rock. Martin and Chandler (1994), Martin (1997), Hoek and Martin (2014) reported $D/C$ ratios between 0.6 and 0.8 for Lac du Bonnet granite. Data compilation by Xue et al. (2014) from published data showed $D/C$ ratios to average at 0.78 for igneous rocks. Rao and Kusunose (1995) reported a $D/C$ ratio of 0.7 for intact Yugawara Andesite.

The $\sigma_{cd}$ threshold, marked by the reversal of the volumetric strain curve, is reached when the increment of axial strain becomes equal to and subsequently exceeded by twice the increment
of diametric strain. In intact rock, this manifests as a coalescence of tensile microcracks, developing since reaching the $\sigma_{ci}$ threshold, into a shear fracture.

A high $D/C$ ratio indicates that the unstable fracturing stage was brief or absent and that the intact veined specimens ruptured shortly after the $\sigma_{cd}$ threshold was reached. In the experiments, the $\sigma_{ci}$ threshold was attained relatively late in the test. Only few AE events were recorded between the onset of dilatancy and the crack damage threshold (see Section 6.8.7). Their interpreted locations did not indicate that a fracture was forming. These suggest that the coalescence of microcracks in the tested specimens of intact veined rock did not occur in the sense of how it is perceived in intact rock.

Based on the experimental results it is concluded that the presence of veins caused high levels of $\sigma_{cd}$ and extremely short stages of unstable fracturing. Similar to the interpretation of the dilatancy (Section 6.8.4), it is interpreted that the specimens of intact veined rock fractured prematurely along veins due to inherent anisotropy.

6.8.6 Roles of Vein Mineralogy, Thickness, and Orientation in Specimen Fracturing

According to the MRMR classification system of Laubscher and Jakubec (2001) (see Appendix A), vein spacing and hardness of the infill material (i.e. vein mineralogy) affect the strength of intact rock blocks. The strength is reduced with increase in frequency and/or with decrease in infill hardness. Brzovic (2010) classified veins being ‘weak’ if they contained less than 1/3 of hard (Mohs hardness $> 4$) infill and were thicker than 2 mm. In analysing experiments on intact veined rock, mineralogy, thickness, and orientation of veins were considered within the context of an individual specimen and by comparing fracture veins between the specimens.

6.8.6.1 Veins in Individual Specimens

Within each specimen of intact veined rock, veins of various mineralogical composition were present, both rich in hard and soft minerals as determined based on Moh’s hardness (MH) scale. Dominant hard minerals (MH$> 4$, as defined by Brzovic (2010)) included quartz (MH=7), plagioclase (MH=6), and k-feldspar (MH=6). Dominant soft minerals (MH$\leq 4$) included anhydrite (MH=3), biotite (MH=2), chlorite (MH=2), chalcopyrite (MH=3½), and muscovite (MH=2½).
Specimen 1 fractured along quartz-rich (65-80%, MH=7) veins which varied in thickness between less than 1 mm and 1.8 mm. Specimen 2 fractured along two veins containing 13-36% plagioclase (MH=6) and 41-46% quartz (MH=7), having thicknesses of 1.2 mm and 1.7 mm. The same specimen contained anhydrite-rich (55% and 90%, MH=3) veins, with one of them being 4.7 mm thick, which did not participate the specimen fragmentation. Specimen 6 fractured along a quartz-rich (70%, MH=7) vein. Specimen 14 fractured along two veins. One was a 1.9 mm thick vein, rich in chalcopyrite (65%, MH=3½); another was a 2.7 mm thick vein, containing anhydrite (25%, MH=3), quartz (70%, MH=7), and chalcopyrite (35%, MH=3½). Specimen 15 fractured on a 0.4 mm thick vein containing 92% anhydrite (MH=3).

From the above summary one can see that the fracturing process during the experiments did not favour veins of particular mineralogy or thickness. The same cannot be said about vein angles.

In all experiments, all veins that participated in the specimen fragmentation were all oriented at an angle of 50° to 78° with respect to the diametric axes of the specimens. In triaxial compression experiments on core specimens of intact rock, fractures tend to form at angles in the range between 50° and 70° (Paterson and Wong 2005). Based on this observation, it can be concluded that during axial loading of an intact veined rock specimen conditions develop inside the specimen that are similar to intact rock. The rupture of the intact veined rock exploits the veins because they are weaker than the host material. However, only veins that fall within the critical orientation range (50° and 70°) get exploited because they are aligned with the natural direction of the shear. Vein mineralogy and thickness appear to play minor roles in fracture propagation.

6.8.6.2 Vein Mineralogy in Relationship to Specimen Peak Strengths

In Section 6.8.6.1 it was shown that vein mineralogy did not determine along which veins the fracture would develop during an experiment. Vein mineralogy, however, demonstrated correlation with the peak strengths attained by the specimens.

For each specimen, average content of hard minerals (MH>4) was computed by summing volumetric contents of hard minerals from fractured veins and dividing the sum by the number of veins. In Figure 6-12, the results are plotted against the peak experimental strengths. It can be seen
that for the tested specimens of intact veined rock, presence of higher content of hard minerals in the veins along the fracture took place allowed the specimens to attain higher peak strengths.

Figure 6-12. Relationship between average volumetric content of hard minerals (Moh’s hardness > 4) for veins participated in fracturing and specimen peak strength.

Both Laubscher and Jakubec (2001) and Brzovic (2010) suggested that harder vein infill increased the strength of the intact rock blocks. Their suggestions were based on qualitative observations. The results presented here demonstrate experimental evidence of this.

6.8.7 Acoustic Emission

Definite correlation between the onset of dilatancy and the onset of AE, which is normally recognized in intact rock experiments (Eberhardt et al. 1998; Heap et al. 2014; 1968; Siratovich et al. 2014), was observed only in one of the experiments (Experiment 4, Specimen 14) on intact veined rock. In the remaining experiments, the onset of AE correlated with the damage initiation stress threshold.

The lack of correlation between the onset of AE and the crack initiation stress threshold in the intact veined specimens was not anticipated prior to testing. Equipment setup is not believed
to be the cause as AE data were harvested from continuous waveform records and different event trigger characteristics and thresholds were tried.

The correlation between the onset of AE and dilatancy presumes that new cracks are starting to form when the $\sigma_{ci}$ threshold is attained. If a specimen contains microcracks prior to an experiment, like the tested specimens of intact veined rock, then its dilation can occur through opening of existing cracks rather than formation of new ones, which would not be expected to be accompanied by AE. The preexisting microcracking, as noted in Section 6.8.1, can be related to in-situ stress in general (Plumb et al. 1984) or be associated with rock mass fracturing that occurred prior to or during the placement of veins.

6.9 Conclusions

Five specimens of intact veined rock were tested in triaxial compression. The experiments were carried out under a stress path with varying confining pressure. The loading mimicked the effects of an approaching caving front in a block cave mine. The experimental results suggested that the behaviour of the specimens was influenced by the presence of veins.

The experiments demonstrated that fracturing of intact veined rock specimens was controlled by veins. In five laboratory triaxial tests, each specimen failed along a single vein or multiple veins. Veins were the weakest mechanical link in the system, and therefore the strength of the veins determined the overall strength of the specimens.

Measurements of diametric and longitudinal P-wave velocities prior to testing suggested that the specimens contained microcracks that were oriented subparallel to the direction of the regional maximum principal stress. Orientation of the specimen in-situ with respect to $\sigma_i$ correlated with the peak strengths. In triaxial experiments, specimens that were oriented subparallel to the direction of the regional major principal stress exhibited lower peak strengths than specimens oriented normal to the direction of regional $\sigma_i$. A linear relationship between the experimental peak strengths and the ratios of diametric to axial P-wave velocities was observed.

Vein mineralogy and thickness did not determine whether any particular vein would participate in specimen fracturing during an experiment. Vein angle with respect to the axial load, however, influenced if the vein fractured. Fractured veins in all experiments were oriented at an angle between $50^\circ$ and $78^\circ$ with respect to the specimen diameter. In triaxial experiments on intact
rock cylindrical specimens normally fail in shear with failure planes oriented at an angle of 50° to 70° with respect to the horizontal axis (20-40° with respect to the axial load).

The experiments demonstrated correlation between mineralogy of the veins involved in the failure and the specimen’s peak strengths. Specimens that ruptured on veins containing higher content of hard minerals (Moh’s hardness of greater than 4) attained higher peak strengths than specimens with lower content of hard minerals. This is consistent with the results of Brzovic (2010).

The crack initiation stress threshold values were consistent between the tested specimens. The $C'/C$ ratios were higher than what are commonly observed in experiments on intact rock. This is attributed to the veined rock fracturing at lower stress levels than intact rock, which is due to the presence of veins.

All experiments demonstrated a short stage of unstable fracturing that is atypical of intact rock. Rupture of the specimens occurred immediately after the crack damage stress thresholds were reached. It is concluded that the presence of veins eliminates the unstable cracking phase in intact veined rock.

Specimens of intact veined andesite from El Teniente did not display significant AE activity prior to reaching their peak strengths. Acoustic emission monitoring demonstrated that unlike compression experiments on intact rock, where the onset of AE activity tends to correlate with specimen dilatancy, for specimens of intact veined rock, the onset of AE correlated with the damage initiation stress. Consequently, in experiments on intact veined rock, AE activity should not be used for detection of specimen dilatancy as it can lead to misleading interpretations.

In the next chapter, experimental results from the tests with various levels of confining pressure are presented and discussed. Unlike the initial tests, which followed a stress path with varying confining pressure, the experiments discussed in Chapter 7 were standard triaxial tests.
Chapter 7
Influence of Different Confining Pressures on the Behaviour of Intact Veined Rock

7.1 Introduction

This chapter presents the results of the compression experiments on five specimens of intact veined rock. Preliminary results were reported by Turichshev and Hadjigeorgiou (2016b). Triaxial tests were carried out under various values of confining pressure. The results of each experiment are described in detail. Similar to the tests presented in Chapter 6, figures are used to visualize the results. The figures include stress-strain data, evolution of acoustic emission events, changes in axial and diametric P-wave velocities, changes in measured volumetric and calculated crack volumetric strain, and AE event locations. Interpretations of the crack closure, crack initiation, and crack damage stress thresholds were made using the methods presented in Chapter 5. These are also shown in the summary figure for each experiment. Findings that stemmed from the experiments are discussed at the end of the chapter.

The experiments involved triaxial testing of Specimens 5, 7, 8, 12, and 19. The experiments were standard triaxial tests. The confining pressure of 2, 5, 30, 45, and 60 MPa was used.

In describing specimen fracturing, veins that were involved in the disassembly of the specimen are referred by their number. The vein numbers for each specimen are as per the numbers provided in Chapter 4.

7.2 Experiment 1, Specimen 7 (30 MPa Confining Pressure)

Axial and diametric P-wave velocities were measured prior to specimen loading. The axial velocity was 4,104 m/s, and the diametric 4,690 m/s. The ratio of the diametric to axial P-wave velocities was 1.14.

The experimental results are summarized in Figure 7-1. The stress-strain response and the AE event development are shown in part a) of the figure. Diametric strain is plotted in part b). Plots of the volumetric and crack volumetric strain are shown in part c) of the figure. Part d) displays the evolution of the axial and diametric P-wave velocities vs. axial strain. Interpreted
locations of the AE events and fractures developed by the specimen during the experiment are visualized in part e).

Figure 7-1. Summary of experimental results from Specimen 7 test.
The experiment was conducted under a confining pressure of 30 MPa. The specimen reached a peak compressive strength of 229.6 MPa. The average tangential Young’s modulus and the Poisson’s ratio were 30.0 GPa and 0.22, respectively.

The fracturing of the specimen occurred along vein #1. The vein was 1.99 mm thick and had an orientation of 54° to the diametric axis of the specimen (Figure 7-2). The composition of the vein included approximately equal proportions of anhydrite and quartz, 24% and 29% respectively, and 37% of muscovite. The specimen exhibited brittle failure. The axial load at the end of the experiment was recorded at 105 MPa.

Figure 7-2. Photographs of Specimen 7 after the experiment.

The total number of AE events recorded during the experiment was 1,116. Out of the total events, location was established for 472 (42%) events. No acoustic events were recorded prior to the specimen reaching its peak strength (Figure 7-1a).

The interpretation of the AE event locations (Figure 7-1e) suggested that the fracture of the specimen was initiated at the specimen boundary, in the lower edge of the fracture plane. The fracture then propagated towards the top end of the specimen.
The crack closure stress threshold ($\sigma_{cc}$) was determined to be approximately 62 MPa. The crack initiation stress ($\sigma_{ci}$) and the crack damage stress ($\sigma_{cd}$) thresholds were determined to be 171 and 228 MPa, respectively (Figure 7-1). The ratio of the stress difference at $\sigma_{ci}$ to the stress difference at peak ($C'/C$) was calculated being equal to 0.71. The ratio of the stress difference at $\sigma_{cd}$ to the stress difference at peak ($D/C$) was calculated being equal to 0.99.

### 7.3 Experiment 2, Specimen 19 (2 MPa Confining Pressure)

Axial and diametric P-wave velocities were measured prior to specimen loading. The axial velocity was 5,020 m/s, and the diametric 4,789 m/s. The ratio of the diametric to axial P-wave velocities was 0.95.

The experimental results are summarized in Figure 7-3. The stress-strain response and the AE event development are shown in part a) of the figure. Diametric strain is plotted in part b). Plots of the volumetric and crack volumetric strain are shown in part c) of the figure. Part d) displays the evolution of the axial and diametric P-wave velocities vs. axial strain. Interpreted locations of the AE events and fractures developed by the specimen during the experiment are visualized in part e).

The experiment was conducted under a confining pressure of 2 MPa. The specimen reached a peak compressive strength of 162.5 MPa. The average tangential Young’s modulus and the Poisson’s ratio were 33.7 GPa and 0.24, respectively. The axial load at the end of the experiment was recorded at 43 MPa.

The specimen exhibited brittle failure. The fracturing of the specimen occurred initially along vein #5 (1.34 mm thick / 28°). Exact mineralogical composition of this vein was unknown. Because the specimen was highly fractured, only two thin sections were prepared, and both did not contain the #5 vein. However, it is known that #5 vein, being mineralized, contained high percentage of chalcopyrite. An average orientation of the fracture was 25° with respect to the diametric axis. Additional sub-vertical fractures also developed. Examination of the specimen showed that these sub-vertical fractures mostly occurred through intact portion of the specimen (Figure 7-4). It is thought that the sub-vertical fractures were induced post-testing, during the extraction of the specimen from the testing cell.
Figure 7-3. Summary of experimental results from Specimen 19 test.

Specimen 19
Confining Stress: 2 MPa
Young’s Modulus, E: 33.7 GPa
Poisson’s Ratio, ν: 0.24
Peak Strength: 162.5 MPa
Crack Closure Stress, $\sigma_{cc}$: 70 MPa
Crack Initiation Stress, $\sigma_{ci}$: 125 MPa
Crack Damage Stress, $\sigma_{cd}$: 158 MPa
Figure 7-4. Photographs of Specimen 19 after the experiment.

The total number of AE events recorded during the experiment was 8,782. Out of the total events, location was established for 905 (10%) events. Approximately 60 events were recorded during the crack closure stage of the experiment (Figure 7-3a). An increased level of AE activity (approximately 300 events) was registered at approximately 90 MPa axial stress. These events appear to be associated with the thick mineralized vein located in the mid-section of the specimen, cutting it at a shallow angle. The majority of the AE events were recorded during the rupture of the specimen (Figure 7-3a). The interpreted locations of the events suggest that the specimen first ruptured on the mineralized sub-horizontal vein.

The $\sigma_{cc}$ threshold was determined to be 70 MPa. The $\sigma_{ci}$ and the $\sigma_{cd}$ thresholds were determined being 125 and 158 MPa, respectively (Figure 7-3). The $C'/C$ and the $D/C$ ratios were calculated being equal to 0.77 and 0.97, respectively.

7.4 Experiment 3, Specimen 8 (60 MPa Confining Pressure)

Axial and diametric P-wave velocities were measured prior to specimen loading. The axial velocity was 3,889 m/s, and the diametric 4,230 m/s. The ratio of the diametric to axial P-wave velocities was 1.09.

The experimental results are summarized in Figure 7-5. The stress-strain response and the AE event development are shown in part a) of the figure. Diametric strain is plotted in part b). Plots of the volumetric and crack volumetric strain are shown in part c) of the figure. Part d)
displays the evolution of the axial and diametric P-wave velocities vs. axial strain. Interpreted locations of the AE events and fractures developed by the specimen during the experiment are visualized in part e).

The experiment was conducted under a confining pressure of 60 MPa, the highest confining pressure in all experiments. The specimen reached a peak compressive strength of 377.1 MPa. The average tangential Young’s modulus and the Poisson’s ratio were 34.0 GPa and 0.16, respectively. The axial load at the end of the experiment was recorded at 289 MPa.

The specimen exhibited gradual failure. The specimen developed several fracture planes oriented between 51° and 69° with respect to the diametric axis of the specimen (Figure 7-6). Fractures fully followed veins #1 (0.30 mm thick / 63°) and #9 (1.26 mm thick / 59°). Partial fracturing occurred along veins #3 (0.40 mm thick / 57°) and #8 (1.41 mm thick / 52°). Veins #1 and #3 had similar mineralogical compositions: 20% anhydrite, 47-50% quartz, 10-12% tremolite, and 6-7% chalcopyrite. The mineralogical composition of veins #8 and #9 could not be established.

The total number of AE events recorded during the experiment was 4,152. Out of the total events, location was established for 1,078 (26%) events. Approximately 150 events were recorded during the crack closure stage of the experiment. Approximately additional 200 events were registered prior to the specimen reaching its peak strength. The majority of the AE events were recorded during the rupture of the specimen (Figure 7-5a). Continuous AE record files for several channels were discovered being corrupt after the experiment, resulting in partial loss of data that were critical to location of the events. This is the reason why relatively few events were located around the time and after the specimen reached the peak strength. The loss of data did not affect AE counts.

The $\sigma_{cc}$ threshold was determined to be 120 MPa. The $\sigma_{ci}$ and the $\sigma_{cd}$ thresholds were determined being 219 and 365 MPa, respectively (Figure 7-5). The $C'/C$ and the $D/C$ ratios were calculated being equal to 0.50 and 0.96, respectively.
Figure 7-5. Summary of experimental results from Specimen 8 test.

Specimen 8
Confining Stress: 60 MPa
Young’s Modulus, E: 34.0 GPa
Poisson’s Ratio, ν: 0.16
Peak Strength: 377.1 MPa
Crack Closure Stress, \( \sigma_{cc} \): 120 MPa
Crack Initiation Stress, \( \sigma_{ci} \): 219 MPa
Crack Damage Stress, \( \sigma_{cd} \): 365 MPa
7.5 Experiment 4, Specimen 5 (45 MPa Confining Pressure)

Axial and diametric P-wave velocities were measured prior to specimen loading. The axial velocity was 4,004 m/s, and the diametric 4,303 m/s. The ratio of the diametric to axial P-wave velocities was 1.07.

The experimental results are summarized in Figure 7-7. The stress-strain response and the AE event development are shown in part a) of the figure. Diametric strain is plotted in part b). Plots of the volumetric and crack volumetric strain are shown in part c) of the figure. Part d) displays the evolution of the axial and diametric P-wave velocities vs. axial strain. Interpreted locations of the AE events and fractures developed by the specimen during the experiment are visualized in part e).
Figure 7-7. Summary of experimental results from Specimen 5 test.

The experiment was conducted under a confining pressure of 45 MPa. The specimen reached a peak compressive strength of 320.1 MPa. The average tangential Young’s modulus and
The Poisson’s ratio were 33.2 GPa and 0.11, respectively. The specimen exhibited brittle failure. The specimen fractured along a single vein 0.15 mm thick, oriented at an angle of 55° with respect to the diametric axis of the specimen (Figure 7-8). This vein was not documented in the DVN of Specimen 5. Its mineralogical composition included 30% anhydrite, 20% quartz, 15% chlorite, and 30% tremolite. The axial load at the end of the experiment was recorded at 78 MPa.

The total number of AE events recorded during the experiment was 770. Out of the total events, location was established for 213 (28%) events. Few AE events were registered prior to the specimen reaching its crack damage stress threshold. Approximately 50 events were registered at the $\sigma_{cd}$ axial stress level. The majority of the AE events were recorded during the specimen rupture.

The $\sigma_{cc}$ threshold was determined to be 50 MPa. The $\sigma_{cl}$ and the $\sigma_{cd}$ thresholds were determined being 188 and 285 MPa, respectively (Figure 7-7). The $C'/C$ and the $D/C$ ratios were calculated being equal to 0.52 and 0.87, respectively.

**Figure 7-8. Photographs of Specimen 5 after the experiment.**

7.6 Experiment 5, Specimen 12 (5MPa Confining Pressure)

Axial and diametric P-wave velocities were measured prior to specimen loading. The axial velocity was 5,243 m/s, and the diametric 4,990 m/s. The ratio of the diametric to axial P-wave velocities was 0.95.
The experimental results are summarized in Figure 7-9. The stress-strain response and the AE event development are shown in part a) of the figure. Diametric strain is plotted in part b). Plots of the volumetric and crack volumetric strain are shown in part c) of the figure. Part d) displays the evolution of the axial and diametric P-wave velocities vs. axial strain. Interpreted locations of the AE events and fractures developed by the specimen during the experiment are visualized in part e).

The experiment was conducted under a confining pressure of 5 MPa. The specimen reached a peak compressive strength of 167.5 MPa. The average tangential Young’s modulus and the Poisson’s ratio were 33.3 GPa and 0.19, respectively. The axial load at the end of the experiment was recorded at 42 MPa.

The specimen failed in two stages. The initial fracture occurred along vein #2 (2.10 mm thick / 74°); the vein transcended the specimen from top to bottom (Figure 7-10). The #2 vein contained 31% anhydrite, 9% quartz, 9% chalcopyrite, 16% k-feldspar, and 26% muscovite. The second fracture occurred along vein #3 (0.79 mm thick / 74°) located in the upper part of the specimen (Figure 7-10). The vein was rich in quartz (90%).

The total number of AE events recorded during the experiment was 5,174. Out of the total events, location was established for 576 (11%) events. No acoustic events were recorded prior to the specimen reaching its peak strength, and the majority of the recorded events were associated with the initial rupture of the specimen (Figure 7-9a).

The $\sigma_{cc}$ threshold was determined to be 61 MPa. The $\sigma_{ci}$ and the $\sigma_{cd}$ thresholds were determined being 132 and 163 MPa, respectively (Figure 7-9). The $C'/C$ and the $D/C$ ratios were calculated being equal to 0.78 and 0.97, respectively.
Figure 7-9. Summary of experimental results from Specimen 12 test.

Specimen 12
Confining Stress: 5 MPa
Young’s Modulus, E: 33.3 GPa
Poisson’s Ratio, v: 0.19
Peak Strength: 167.5 MPa
Crack Closure Stress, $\sigma_{cc}$: 61 MPa
Crack Initiation Stress, $\sigma_{ci}$: 132 MPa
Crack Damage Stress, $\sigma_{cd}$: 163 MPa

AE Event Locations
7.7 Summary of Experimental Results

The key experimental results are summarized in Table 7-1. Every tested specimen fractured in a brittle manner. In 4 out of 5 experiments, the drop of axial stress from its peak to the final value was sudden. In the experiment with the highest confining pressure (60 MPa) a gradual post-peak response was observed.

The post-experimental examination of the specimens revealed that all fractures occurred on pre-existing veins. The tests demonstrated that veins represented the weakest mechanical link in the system, controlling fracturing of the specimen.

Table 7-1. Summary of the test results from the experiment using various levels of confining pressure. The results are ordered by increasing value of the confining pressure \( (p_c) \) used in the experiment.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Sp.19</th>
<th>Sp.12</th>
<th>Sp.7</th>
<th>Sp.5</th>
<th>Sp.8</th>
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</thead>
<tbody>
<tr>
<td>Diametric P-wave velocity (m/s)</td>
<td>4,789</td>
<td>4,990</td>
<td>4,690</td>
<td>4,303</td>
<td>4,230</td>
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<td>Axial P-wave velocity (m/s)</td>
<td>5,020</td>
<td>5,243</td>
<td>4,104</td>
<td>4,004</td>
<td>3,889</td>
</tr>
<tr>
<td>Diam. vel. / axial vel. ratio</td>
<td>0.95</td>
<td>0.95</td>
<td>1.14</td>
<td>1.07</td>
<td>1.09</td>
</tr>
<tr>
<td>Direction of regional ( \sigma_1 ) with respect to specimen long axis</td>
<td>( \parallel )</td>
<td>( \parallel )</td>
<td>( \perp )</td>
<td>( \perp )</td>
<td>( \perp )</td>
</tr>
<tr>
<td>Parameter</td>
<td>Sp.19</td>
<td>Sp.12</td>
<td>Sp.7</td>
<td>Sp.5</td>
<td>Sp.8</td>
</tr>
<tr>
<td>-------------------------------</td>
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<td>-------</td>
<td>-------</td>
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<tr>
<td>Confining pressure, $p_c$ (MPa)</td>
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<td>5</td>
<td>30</td>
<td>45</td>
<td>60</td>
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<tr>
<td>Young’s modulus, $E$ (GPa)</td>
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<td>33.2</td>
<td>34.0</td>
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<tr>
<td>Poisson’s ratio, $\nu$</td>
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<td>0.19</td>
<td>0.22</td>
<td>0.11</td>
<td>0.16</td>
</tr>
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<td>Bulk modulus, $K$ (GPa)</td>
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<td>17.9</td>
<td>14.2</td>
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<tr>
<td>Shear modulus, $G$ (GPa)</td>
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<td>15.0</td>
<td>14.7</td>
</tr>
<tr>
<td>Peak strength, $\sigma_f$ (MPa)</td>
<td>162.5</td>
<td>167.5</td>
<td>229.6</td>
<td>320.1</td>
<td>377.1</td>
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<td>Crack closure stress, $\sigma_{cc}$ (MPa)</td>
<td>70</td>
<td>61</td>
<td>62</td>
<td>50</td>
<td>120</td>
</tr>
<tr>
<td>Crack initiation stress, $\sigma_{ci}$ (MPa)</td>
<td>125</td>
<td>132</td>
<td>171</td>
<td>188</td>
<td>219</td>
</tr>
<tr>
<td>$C’/C = (\sigma_{ci} - p_c) / (\sigma_f - p_c)$</td>
<td>0.77</td>
<td>0.78</td>
<td>0.71</td>
<td>0.52</td>
<td>0.50</td>
</tr>
<tr>
<td>Crack damage stress, $\sigma_{cd}$ (MPa)</td>
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<td>163</td>
<td>228</td>
<td>285</td>
<td>365</td>
</tr>
<tr>
<td>$D / C = (\sigma_{cd} - p_c) / (\sigma_f - p_c)$</td>
<td>0.97</td>
<td>0.97</td>
<td>0.99</td>
<td>0.87</td>
<td>0.96</td>
</tr>
</tbody>
</table>

### 7.8 Discussion

#### 7.8.1 P-Wave Velocities

The results of P-wave velocity measurements were similar to the results of and observations made from the first set of experiments (Section 6.8.1). In specimens which had long axes oriented sub-parallel to the direction of the regional major principal stress ($\sigma_1$), the ratios of diametric to axial velocities were lower than 1. The P-wave velocity ratios were consistently greater than 1 in the specimens whose long axes were normal to the direction of $\sigma_1$. The significance of these observations were discussed in Chapter 6.

#### 7.8.2 Elastic Moduli

Elastic moduli measured in the experiments were similar to the values measured in previous tests (Chapter 6). The modulus values ranging 30-34 GPa were lower than 50-60 GPa values that are commonly reported for CMET (see Table 3-1, Chapter 3). Potential explanations for the discrepancy were discussed in Section 6.8.3 of Chapter 6.

#### 7.8.3 Dilatancy and Crack Initiation

Values of $C’/C$ ratios in three specimens tested under a confining pressure of 30 MPa or less were higher than one would expect for intact rock, ranging between 0.71 and 0.78 (Table 7-1). This is consistent with the ratios calculated for the specimens tested with the confining pressure of 12 MPa (Chapter 6). At confining pressures of 45 and 60 MPa, $C’/C$ ratios were 0.52 and 0.50, at
the level that is consistent with intact rock (Brace et al. 1966; Scholz 1968) and intact andesite (Heap et al. 2014; Rao and Kusunose 1995; Siratovich et al. 2014). The results of the experiments suggest that under a confining pressure of \( \leq 30 \text{ MPa} \) veins influenced the crack initiation stress threshold in the intact veined rock specimens.

The results of the standard triaxial experiments further reinforced the conclusions from the initial experiments (Section 6.8.4). The conclusion was that the elevated values of the \( C'/C \) ratio were caused by lowered values of the peak strength and not by elevated values of \( \sigma_{ci} \). If opposite was true and the occurrence of veins within specimens of intact veined rock caused a delay in specimen dilation under low confining pressure (higher \( C' \)), than the same should have been observed in the experiments under high confining pressure. Furthermore, higher confining pressure would further prevent the cracks from opening, increasing the \( C'/C \) ratio. However, the results of the experiments at high confining pressure displayed the opposite. The \( C'/C \) ratio decreased, suggesting that at higher confining pressure veins are unlikely to restrain specimen dilation beyond the \( \sigma_{ci} \) value of intact rock.

The relationship between the \( \sigma_{ci} \) and the confining pressure (Figure 7-11) was linear. Experimental data published by Brace et al. (1966) for marble and aplite, by Scholz (1968) for Westerly granite, and by Hoek and Martin (2014) for Lac du Bonnet granite also showed that variations of crack initiation stress vs. confining pressure fitted linear relationships.

What is also interesting about Figure 7-11 is the intercept of the ordinate axis. The relationship implies that in an uniaxial test (\( p_c = 0 \text{ MPa} \)), the crack initiation stress threshold is expected to be around 123 MPa. Average uniaxial compression strength of CMET andesite is 125 MPa. This suggests that the period between crack initiation and onset of dilatancy in UCS experiments on intact veined rock can be extremely small.
Figure 7-11. Relationship between crack initiation stress threshold and confining pressure.

7.8.4 Onset of Unstable Fracturing

Similar to the results from the experiments discussed in Chapter 6, the specimens demonstrated high $D/C$ ratio values, ranging between 0.87 and 0.99 (Table 7-1). In comparison, in experiments on intact andesite (Rao and Kusunose 1995) and intact rock in general (Hoek and Martin 2014; Martin and Chandler 1994; Martin 1997; Xue et al. 2014), the $D/C$ ratios of 0.6-0.8 are normally observed.

The stage of unstable fracturing in the intact veined specimens was typically short. Only in the experiment with the confining pressure of 60 MPa, the response of the veined specimen between the crack damage stress threshold and rupture was observed. Out of the five experiments, veins in Specimen 8 had the least impact on the failure of the specimen, allowing it to behave more like a specimen of intact rock. The high confining pressure during the test was probably the key reason why this behaviour was captured.
7.8.5 Effects of Vein Mineralogy and Orientation

Four out of five specimens in standard triaxial tests fractured along veins that were inclined between 54° and 74° with respect to the diametric axes of the specimens. These results were consistent with the initial tests. They were also consistent with general results of triaxial tests on intact rock in which shear fractures tend to form at an angle between 50° and 70° from the horizontal (Paterson and Wong 2005).

Specimen 19 fractured on a 1.3 mm thick chalcopyrite-rich vein that was inclined 28° from the horizontal. This was abnormal in comparison to all other fractures developed in the tested specimens of intact veined rock. Examination of Specimen 19 and its DVN showed that all mapped veins having an angle that was preferential to fracturing were small and positioned at the ends of the specimen. These veins mostly had quartz-rich mineral composition. Their activation was problematic during the experiment. Vein #5 along which the fracture occurred, however, was positioned mid-height of the specimen and was relatively weak, being rich in chalcopyrite. It is concluded that the specimen ruptured along vein #5 due to its location, its mineral composition dominated by chalcopyrite (soft mineral), and due to absence of other veins that could provide a viable alternative for the development of the fracture.

7.8.6 Strength Envelope

The experimental results were clear in demonstrating the effects of mineral veins on the strength envelope of CMET. The Hoek-Brown $m_i$ constant (Hoek and Brown 1997) is an indicator of the degree to which confining pressure affects the strength of intact rock. Rocks with a higher value of $m_i$ constant will demonstrate greater strength gains with confinement than rocks with a lower $m_i$. Figure 7-12 illustrates the stress-strain curves and the associated strength envelope. The value of the Hoek-Brown $m_i$ parameter was calculated being equal to 9.6. The calculated value of the $m_i$ constant is in agreement with the value of 9.1 that is generally quoted for the El Teniente’s CMET andesite (e.g. Brzovic 2010).
Figure 7-12. Axial stress vs. axial strain curves from the triaxial experiments are plotted in a). Peak strengths in the $\sigma_1$-$\sigma_3$ space (peak strength vs. confining pressure) are shown in b). The Hoek and Brown (1997) strength envelope is shown as the red dashed line in b).

7.8.7 Acoustic Emission

Similar to the tests under the “caving” stress path, the standard triaxial experiments demonstrated good correlation between AE events and the damage initiation stress thresholds. In three experiments, with confining pressures of 5, 30, and 45 MPa, AE activity was not recorded until peak strengths. In the experiment with the confining pressure of 2 MPa, few hundred events were recorded during the crack closure and elastic stages but very few between the time the specimen reached the crack initiation stress threshold and the rupture of the specimen. Only in the experiment with the confining pressure of 60 MPa (Specimen 8) the count of acoustic events increased gradually by approximately 200 events following the $\sigma_{ci}$ and abruptly at the peak stress.

7.9 Brittle Failure of Intact Veined Rock

Brittle failure of intact rock and of intact andesite in compression were reviewed and discussed in Chapter 5. Figure 5-1 was used to illustrate generic behaviour of intact rock. The experiments on intact veined rock, described in this chapter and in Chapter 6, demonstrated that
the behaviour of intact veined rock differed from the one of intact rock. The differences in the behaviour exhibited by intact veined rock in compression are illustrated in Figure 7-13. Key characteristics of the behaviour include:

- Higher crack initiation stress threshold ($\sigma_{ci}$) with respect to peak strength (75%) than observed in typical intact rock (40%);
- Higher crack damage stress threshold ($\sigma_{cd}$) with respect to peak strength (95%) than observed in typical intact rock (80%);
- Short duration of the unstable crack growth stage (Stage IV);
- Correlation between the onset of AE activity and $\sigma_{cd}$ rather than $\sigma_{ci}$, which is typical of intact rock; and,
- Veins control the rupture of the specimen.

Figure 7-13. Brittle fracture of intact veined rock in compression.
7.10 Conclusions

The primary objective of the experiments was to develop an understanding and to characterize the behaviour of intact veined rock under compressive loads. The collected experimental data was used to develop an approach for numerical modelling of intact veined rock, which was the secondary objective of this work.

The experiments consisted of triaxial tests with standard loading procedure of the specimens. Five specimens of intact veined rock were tested under confining pressures of 2, 5, 30, 45, and 60 MPa. The results of the experiments demonstrated many similarities to the results of “caving” stress path tests.

Experiments demonstrated that veins controlled specimen rupture. Brittle failure was observed in experiments with confining pressure of 45 MPa or less. In the test with the confining pressure of 60 MPa, a specimen of intact veined andesite displayed a strain-softening behaviour after reaching its peak strength.

It was shown that the crack initiation stress threshold had a linear relationship with the confining pressure; \( \sigma_{ci} \) increased with confining pressures. This was consistent with observations made in experiments on intact rock (e.g. Brace et al. 1966; Scholz 1968). Moreover, \( \sigma_{ci} \) values from the experiments using the “caving” stress path appeared to fit the established relationship as well.

Based on experimental results, the Hoek-Brown \( m_i \) constant was calculated to be 9.6, which was consistent with other published values for El Teniente’s CMET lithology (e.g. Brzovic 2010). The \( m_i \) value is low for a crystalline igneous rock (andesite). It is concluded that this is the direct result of veins being present in the specimens, acting as weak mechanical features.

In experiments with the confining pressure of 30 MPa or less, the \( C'/C \) ratios were calculated to be above 0.7, higher than expected in comparison to intact rock. It is concluded that high \( C'/C \) values resulted from lower peak strengths than one would estimate based on the values of the crack initiation stress thresholds. In experiments with the confining pressure of 45 and 60 MPa, the \( C'/C \) ratios were near 0.5, comparable to intact rock.
Given the in-depth analyses of the experimental results and the developed understanding of the behaviour of intact veined rock under stress, the process of modelling of the experiments could commence. The next chapter provides background information that is necessary for understanding the numerical modelling approaches employed in this work.
Chapter 8
Approach to Numerical Modelling of Intact Veined Rock
Using the Synthetic Rock Mass Method

8.1 Introduction

This chapter describes theory behind the numerical simulations used for modelling of the experiments on intact veined rock. The simulations employed the Synthetic Rock Mass (SRM) modelling approach. SRM is introduced at the beginning of the chapter. Its ‘classic’ implementation using Bonded Particle Models (BPM) is presented. BPM and SRM modelling with Particle Flow Code in 3D (PFC3D) is described in detail, including key aspects of particle interactions and bonding, model construction of intact and intact veined specimens, specimen loading, and monitoring of the results. Discussion of limitations associated with the use of BPM and the rationale for the use of Bonded Block Models (BBM) follows. The chapter then presents an approach developed for SRM modelling of intact veined rock using the Distinct Element Code in 3D (3DEC) software.

Given the limited number of laboratory tests carried out on intact veined rock specimens and general limitations of testing, numerical modelling was one approach that could be used to further investigate the behaviour of intact veined specimens. It was not obvious that traditional continuum modelling techniques could capture the observed behaviour controlled by veins. The synthetic rock mass approach, however, appeared promising for modelling of inhomogeneous intact veined rock.

SRM modelling has not been previously used to model intact veined rock. This research, therefore, allowed us to evaluate the applicability of the method, as well as to provide insight into the behaviour of intact veined rock. The challenge of numerical modelling of intact veined rock was the absence of comprehensive information on how to model veins within the SRM method. This was further compounded by the lack of quality or laboratory data to allow one to confirm the results. This research, therefore, also provided a unique opportunity in allowing us to compare the modelling results to quality laboratory data.
8.2 Synthetic Rock Mass

Originally developed for jointed rock (Mas Ivars et al. 2011; Pierce et al. 2009; Sainsbury et al. 2008), SRM relies on numerical modelling designed to simulate the response of the rock mass to loading on a scale comparable to its representative elementary volume. SRM explicitly represents both intact rock blocks, which can fracture during the experiment, and discontinuities along which deformations can occur. The main advantage of the approach is that the rock mass behaviour is not prescribed beforehand via a constitutive model but is an emergent product of a simulation. Therefore, the SRM is potentially an improved technique to approximate the mechanical behaviour of a rock mass than traditional approaches of degrading intact properties based on rock mass classification.

Synthetic rock mass relies on the use of the discrete element method (DEM) which was first introduced by Cundall (1971). In most SRM applications to date, the intact rock has been modelled using bonded particle models in which circular (2D) or spherical (3D) particles are bonded together at their contacts to simulate solid rock. Potyondy and Cundall (2004) pioneered the use of BPMs for modelling of rock using Itasca’s Particle Flow Code 2/3D (PFC2D and PFC3D) software. Since its emergence, bonded particle modelling has become a popular method for modelling rock behaviour.

More recently, Grain Based Models (GBM) in 2D (e.g. Damjanac et al. 2007; Gao 2013; Lan et al. 2010; Nicksiar and Martin 2014) and bonded block models in 3D (e.g. Garza-Cruz et al. 2014) were used for SRM simulations. Block-based models display a number of advantages over particle-based models, and their popularity is growing in the industry.

Discontinuities in SRM models are typically implemented by changing bonding characteristics between particles (or blocks) whose contacts align with the discontinuities. These characteristics can include bonding models, bond strengths or bond behaviours.

Geometric models for discontinuities can be generated by various means. For rock mass scale simulations, Discrete Fracture Network (DFN) realizations are normally used (Pierce et al. 2009). In cases where discontinuity or vein geometries can be readily observed (e.g. small intact specimens), deterministically-generated geometries are appropriate.
8.3 Modelling Approach

Numerical modelling of the experiments on intact veined rock closely followed the laboratory testing of the specimens. Two phases of modelling were performed, one per each set of the experiments. Bonded particle SRM models using PFC3D numerical code were used to model the experiments in which loading was based on the “caving” stress path. The laboratory tests with various confining pressures were modeled using the SRM approach based on bonded blocks, utilizing the 3DEC numerical code. The change in the use of the numerical code was due to a number of potential advantages of the bonded block models over the bonded particle models, which are discussed in Sections 8.5 and 8.6.

A general modelling methodology was identical for both bonded particle and bonded block models. It can be divided into the following steps:

1. Create a numerical specimen to represent a specimen of intact rock.
2. Using numerical experiments that modeled uniaxial compression, direct tension, and triaxial compression tests, obtain microscopic scale parameters such that the target macroscopic scale responses are reproduced. This is a calibration phase for the intact material in SRM models.
3. Build numerical specimens of intact veined rock specimens tested in the laboratory using the constructed discrete vein networks (Chapter 4).
4. Based on the results of one laboratory experiment, calibrate microscopic scale properties of veins such that the macroscopic scale response is obtained.
5. Model the remaining laboratory experiments based on the calibration parameters of Step 4.

8.4 PFC3D Modelling

8.4.1 Background

Particle Flow Code (PFC) is a numerical simulation software based on the distinct element method. Two-dimensional and three-dimensional, respectively called PFC2D and PFC3D, versions of the software are in use. PFC3D is used to simulate the mechanical behaviour of a system comprised of a collection of rigid (incompressible) spherical particles, called balls. Distinct particles in a PFC model displace independent of each other, interacting among themselves only at finite-sized soft contacts formed between the particles (Figure 8-1a). Contacts are characterized
by finite normal and shear stiffness. The mechanical behaviour of a PFC system is described in terms of movement of each particle and the inter-particle forces and moments acting at each contact point. The relationship between particle motion and the forces causing the motion are based on the Newton’s laws of motion (Itasca 2008b; Potyondy and Cundall 2004).

Simulation of more complex behaviours in PFC can be achieved by bonding of the particles at their contacts (Itasca 2008b). While various types of bonding are possible, bonded particle models with parallel bonds (PB) are generally used for modelling of intact rock as described by Potyondy and Cundall (2004). PFC documentation (Itasca 2014) describes parallel bonds being able to provide “mechanical behaviour of a finite-sized piece of cement-like material deposited between the two contacting pieces.” Parallel bonds allow particles to resist relative movement as well as rotation (Figure 8-1b). Bonds can become broken when inter-particle forces or moments exceed the bond strength. This allows one to model formation of cracks that may cause blocks to fragment, which is pertinent to modelling of rock.

![Illustration of a non-bonded (a) and bonded with parallel bonds (b) particulate material in PFC (after Cho et al. 2007).](image)

In addition to spherical particles, PFC3D can use clumps, which are rigid collections of multiple spherical particles that are ‘glued’ together to form super-particles (Itasca 2014). Particles making up a clump object are called pebbles. Pebbles within a clump can overlap each other,
creating a complex irregular shape. Mechanically, a clump behaves like a single rigid body. Clumps are not allowed to break during numerical simulations regardless of the forces acting on them. Both balls and clumps can coexist in a PFC3D model. Just like balls, clumps can be bonded to other clumps or balls.

Clumps in PFC3D can be created in a number of ways. One approach is to create a set of clump templates and then use the templates in model construction. Another approach is to create a model made of particles and then substitute some or all particles with clumps. Alternatively, spherical particles can be joined together to form clumps.

PFC3D also makes use of wall objects. A wall is a faceted manifold surface defined by a mesh (Itasca 2014). In PFC3D, a wall interacts with balls and clumps but not with other walls. A wall can move by following prescribed velocity and rotation, but it does not obey the equations of motion (Itasca 2014). The main purpose of walls is to provide model boundaries.

8.4.2 Contacts

When two particles are sufficiently close to each other in a PFC model, a particle contact is formed (Figure 8-1). Two particles interact with each other at the contact by transmitting forces and moments (Figure 8-2). Contact models govern particle interactions using force-displacement relationships. The interaction is expressed in terms of a set of internal parameters that can be described by rheological components as shown in Figure 8-3 (Itasca 2014). Wall-particle contacts behave similarly to inter-particle contacts.
8.4.3 PFC Parallel Bonded Particle Material

Bonded particle materials in PFC rely on the use of various contact models. Parallel bonds are most commonly used for particle bonding. A parallel bonded (PB) material is a granular assembly in which all inter-particle contacts are bonded using the Linear Parallel Bond Contact Model (Itasca 2014). Smooth joint (SJ) interfaces can be inserted into the PB material to simulate smooth inter-particle contacts. Using smooth joints one can model behaviour of real joints, which is relevant to SRM. The insertion of smooth joints is done by identifying contacts near the interface and replacing their contact model with the Smooth Joint Contact Model (Itasca 2014).

In order to understand the behaviour of PB PFC material, one needs to understand the contact behavior of unbonded particles. Linear Contact Model (Itasca 2014) is used as a contact
model for the particles in unbonded state. The parallel bond component acts in parallel with the linear component (Itasca 2014).

It should be noted that the discussion presented in this section is relevant to the specifics of the models developed during this research. For example, descriptions of the contact models in Sections 8.4.3.1, 8.4.3.2, and 8.4.3.3 do not provide full details of contact interactions; only parts that are applicable to the numerical simulations of the experiments on the intact veined rock specimens are described. For example, components of viscous damping (Itasca 2014) are not included in the discussions as viscous damping was disabled in the PFC3D models. Additionally, the description of contact models is limited only to particle-particle contacts. The behaviour of contacts with wall objects are similar to inter-particle contacts.

**8.4.3.1 Linear Contact Model**

The linear contact model provides linear elastic frictional behaviour without tension. It is assumed that the inter-particle contact has vanishingly small area (contact diameter $D_c \to 0$), and that only forces can be transmitted. The contacting particles are free to rotate, and the contact moment ($M_c$) equals zero (Itasca 2014).

The behaviour of the linear contact model is illustrated in Figure 8-4. The contact force ($F_c$) is equal to the linear force ($F^l$), which is generated by linear springs in the normal and shear direction, having stiffnesses of $k_n$ and $k_s$, respectively. The contact is only active when the surface gap ($g_s$) is less than or equal to zero. The contact cannot sustain tension. Slip occurs when a Coulomb limit, controlled by the friction coefficient ($\mu$), of the shear force ($F^l_s$) is exceeded (Itasca 2014).
Figure 8-4. Behaviour and rheological components of the linear contact model (after Itasca 2014).

Figure 8-5 illustrates the force-displacement law of the linear contact model. The linear normal force \( F_n^l \) is computed at the beginning of each timestep based on the surface gap \( g_s \) and the normal stiffness \( k_n \):

\[
F_n^l = \begin{cases} 
  g_s k_n, & g_s < 0 \\
  0, & g_s \geq 0 
\end{cases}
\]  

(Eq. 8-1)

Based on the normal force, a trial shear force \( F_s^* \) is computed as follows, given that \( g_s < 0 \):

\[
F_s^* = (F_n^l)_0 - k_s \Delta \delta_s
\]  

(Eq. 8-2)

where \( (F_n^l)_0 \) is the shear force at the beginning of the timestep and \( \Delta \delta_s \) is the incremental shear displacement. A contact shear strength \( F_s^\mu \) is computed based on the linear normal force and the friction coefficient \( \mu \):

\[
F_s^\mu = -\mu F_n^l
\]  

(Eq. 8-3)

The linear shear force \( F_s^l \) is updated as follows:

\[
F_s^l = \begin{cases} 
  F_s^*, & |F_s^*| \leq F_s^\mu \\
  F_s^\mu, & |F_s^*| > F_s^\mu, \text{ slip} 
\end{cases}
\]  

(Eq. 8-4)

The contact slip occurs when the magnitude of the linear shear force exceeds the shear strength.
8.4.3.2 Linear Parallel Bond Contact Model

A cemented collection of grains can be modelled in PFC using parallel bonds which allow transmission of both force and moment (Potyondy and Cundall 2004). When installed at a contact, the PB component acts in parallel with the linear component (Section 8.4.3.1). Presence of a parallel bond does not preclude the possibility of slip between the particles. PFC documentation gives a description of how a parallel bond can be visualised (Itasca 2014):

“A parallel bond can be envisioned as a set of elastic springs with constant normal and shear stiffnesses, uniformly distributed over a circular (in 3D) cross section lying on the contact plane and centered at the contact point. These springs act in parallel with the springs of the linear component. Relative motion at the contact, occurring after the parallel
bond has been created, causes a force and moment to develop within the bond material. This force and moment act on the two contacting pieces, and can be related to maximum normal and shear stresses acting within the bond material at the bond periphery. If either of these maximum stresses exceeds its corresponding bond strength, the parallel bond breaks, and the bond material is removed from the model along with its accompanying force, moment and stiffnesses.”

The behaviour of the linear parallel bond contact model is illustrated in Figure 8-6. The normal \( F_n^l \) and shear \( F_s^l \) forces of the linear component \( F^l \) were described in Section 8.4.3.1. The parallel component is active only when the contact is in the bonded state.

The parallel bond normal \( F_n \) and shear \( F_s \) forces are linear elastic and generated by linear springs in the normal and shear direction, having stiffnesses of \( k_n \) and \( k_s \), respectively. The parallel bond breaks if either its normal tensile strength \( \sigma_{c} \) or shear strength, a function of cohesion \( \bar{c} \) and friction \( \bar{\phi} \), is exceeded, at which time the contact becomes unbonded.

\[
F_{c} = F^l + \bar{F}, M_c = \bar{M}
\]

**Figure 8-6. Behaviour and rheological components of the linear parallel bond contact model (after Itasca 2014).**

Figure 8-7 illustrates the force-displacement law of the parallel bond component. The parallel bond force is resolved into a normal and shear forces, and the parallel bond moment \( \bar{M} \) is resolved into a twisting \( \bar{M}_t \) and bending \( \bar{M}_b \) moments (Itasca 2014).
Figure 8-7. Force-displacement behaviour for the parallel bond force and moment: normal force vs. PB surface gap (a), shear force vs. shear displacement (b), twisting moment vs. twist rotation (c), and bending moment vs. bend rotation (d) (after Itasca 2014).

The bond’s cross-sectional properties are updated at the beginning of the timestep. The bond radius ($\bar{R}$) is calculated as a scaled minimum of particle radii:

$$\bar{R} = \bar{\lambda} \min(R^{(1)}, R^{(2)}) \quad \text{(Eq. 8-5)}$$

where $\bar{\lambda}$ is the radius multiplier. The area of the parallel bond ($\bar{A}$) is calculated as:

$$\bar{A} = \pi \bar{R}^2 \quad \text{(Eq. 8-6)}$$
The moment of inertia ($\bar{I}$) and the polar moment of inertia ($\bar{J}$) of the parallel bond cross-section are calculated as:

$$\bar{I} = \frac{1}{4} \pi \bar{R}^4$$  \hspace{1cm} (Eq. 8-7)

$$\bar{J} = \frac{1}{4} \pi \bar{R}^4$$  \hspace{1cm} (Eq. 8-8)

The parallel component normal force is updated based on the normal force at the beginning of the timestep ($\bar{F}_n$), relative normal displacement increment ($\Delta \delta_n$), bond area, and normal stiffness:

$$\bar{F}_n = (\bar{F}_n)_0 + \bar{k}_n \bar{A} \Delta \delta_n$$  \hspace{1cm} (Eq. 8-9)

The parallel component shear force is updated based on the shear force at the beginning of the timestep ($\bar{F}_s$), relative shear displacement increment ($\Delta \delta_s$), bond area, and shear stiffness:

$$\bar{F}_s = (\bar{F}_s)_0 - \bar{k}_s \bar{A} \Delta \delta_s$$  \hspace{1cm} (Eq. 8-10)

The twisting moment is updated based on the following equation:

$$\bar{M}_t = (\bar{M}_t)_0 - \bar{k}_s \bar{J} \Delta \theta_t$$  \hspace{1cm} (Eq. 8-11)

where $(\bar{M}_t)_0$ represents the twisting moment at the beginning of the timestep and $\Delta \theta_t$ represents the relative twist-rotation increment.

The bending moment is updated based on the following equation:

$$\bar{M}_b = (\bar{M}_b)_0 - \bar{k}_n \bar{I} \Delta \theta_b$$  \hspace{1cm} (Eq. 8-12)

where $(\bar{M}_b)_0$ represents the bending moment at the beginning of the timestep and $\Delta \theta_b$ represents the relative bend-rotation increment.

The maximum normal ($\bar{\sigma}$) and shear ($\bar{\tau}$) stresses are computed based on the following relationships:

$$\bar{\sigma} = \frac{\bar{F}_n}{\bar{A}} + \bar{\beta} \frac{|\bar{M}_b|}{\bar{I}}$$  \hspace{1cm} (Eq. 8-13)

$$\bar{\tau} = \frac{|\bar{F}_s|}{\bar{A}} + \bar{\beta} \frac{|\bar{M}_t|}{\bar{J}}$$  \hspace{1cm} (Eq. 8-14)
where $\bar{\beta} \in [0,1]$, the moment contribution factor (Potyondy 2011), is used to control the amount the moment has on the normal and shear stresses of the parallel bond.

The failure envelope of the parallel bond is illustrated in Figure 8-8. If the normal tensile stress exceeds the bond’s tensile strength ($\bar{\sigma}_c$), the bond breaks in tension. If the bond has not been broken in tension, the shear strength limit is tested. The bond breaks in shear if its shear stress exceeds the shear strength ($\bar{\tau}_c$) which is given by

$$\bar{\tau}_c = \bar{\sigma} - \frac{\bar{F}_n}{A} \tan \phi$$  \hspace{1cm} (Eq. 8-15)

![Figure 8-8. Parallel bond failure envelope (after Itasca 2014).](image)

When a parallel bond breaks, its forces and moments are set to zero, the bond becomes unbonded, and its behaviour reverts to the one of the linear bond contact model (Section 8.4.3.1).

### 8.4.3.3 Smooth Joint Contact Model

The smooth joint (SJ) contact model allows simulation of the behaviour of a planar frictional or bonded interface with dilation, regardless of the local particle contact orientation along the interface (Itasca 2008b; Itasca 2014). A SJ contact is installed between all particles that are positioned on the opposite sides of the joint as illustrated in Figure 8-9. A SJ contact cannot be installed between particles and walls.
The behaviour of the smooth joint contact model is illustrated in Figure 8-10. When bonded, a smooth joint contact interface exhibits the linear elastic behaviour. Once the strength limit of the interface is exceeded, the bond breaks and the interface becomes unbonded, at which time its behaviour reverts to the one that is similar to the behaviour of the linear contact model (Section 8.4.3.1) with dilation. The interface of the SJ contact model does not resist relative rotation (Itasca 2014).

\[
F_c = F^l, M_c = 0
\]

Figure 8-10. Behaviour and rheological components of the smooth joint contact model (after Itasca 2014).

The area (A) of the smooth joint cross-section is given by:

\[
A = \pi R^2
\]  
(Eq. 8-16)
where $R$ represents the interface radius. The radius is a scaled minimum of particle radii:

$$R = \lambda \min(R^{(1)}, R^{(2)})$$  \hspace{1cm} (Eq. 8-17)

where the $\lambda$ parameter is the joint radius multiplier.

Because the smooth joint orientation is not aligned with the normal of the contact, the inter-particle timestep relative displacement is resolved into the normal ($\Delta \delta_n$) and shear ($\Delta \delta_s$) components relative to the plane of the SJ interface. The force-displacement behaviour of a bonded smooth joint contact is shown in Figure 8-11.

![Figure 8-11](image)

Figure 8-11. Force-displacement behaviour for the smooth joint contact in a bonded state: normal force vs. normal displacement (a), shear force vs. shear displacement (b), and strength envelope (c) (after Itasca 2014).
The SJ normal force is calculated based on the following relationship:

\[ F_n = (F_n)_0 + k_n A \Delta \delta_n \]  
(Eq. 8-18)

where \((F_n)_0\) is the SJ normal force at the beginning of the timestep.

The trial shear force \(F_s^*\), taking into account the shear force at the beginning of the timestep \(((F_s)_0)\), and the shear strength \(F_s^\mu\) are computed as follows:

\[ F_s^* = (F_s)_0 - k_s A \Delta \delta_s \]  
(Eq. 8-19)

\[ F_s^\mu = -\mu F_n \]  
(Eq. 8-20)

Once the forces are calculated, the SJ bonding state is established. If the normal force exceeds the maximum tensile force \(F_n \geq \sigma_c A\), where \(\sigma_c\) is the bond normal strength, the bond becomes broken in tension. If the trial shear force exceeds the maximum shear force \(F_s^* \geq \tau_c A\), where \(\tau_c\) is the bond shear strength, the bond becomes broken in shear. If the bond breaks, its normal and shear forces are set to zero. If the bond remains intact, its shear force is set to the calculated trial shear force.

In the unbonded state, the behaviour of a SJ contact is similar to the one of the linear contact model described in Section 8.4.3.1. Unlike the linear contact model, the smooth joint contact model includes joint dilation. Joint dilation occurs only if the contact is slipping \((F_s^* > F_s^\mu)\). When slip occurs, the SJ normal force is updated as follows:

\[ F_n = F_n + \left( \frac{|F_s^*| - F_s^\mu}{k_s} \right) k_n \tan \psi \]  
(Eq. 8-21)

where \(\psi\) is the dilation angle.

**8.4.4 Construction of Intact Rock Specimen**

Construction of a bonded particle specimen in PFC3D was accomplished in three stages. First, an initial particle assembly was generated. Second, the particle assembly was converted into a clumped assembly. The reasons for use of a clump-based specimen are discussed in Section 8.5. Third, parallel bonds were installed at all contacts, after which the resulting bonded particle specimen was ready for testing.
8.4.4.1 Particle Assembly Generation

Specimens were generated using the radius expansion technique as described in Itasca (2008c). The specimen volume, a 125 mm high 50 mm diameter cylinder, was defined by wall objects as shown in Figure 8-12a. The walls were used to define specimen volume for ball placement and as a jacket and platens during specimen loading.

The enclosed volume was filled with randomly-positioned balls. Ball radii varied according to a uniform distribution, bound by minimum and maximum values of 0.75 and 1.25 mm. Balls were generated with their radii equal to half of the target value. This was done to ensure that ball overlaps were avoided during their placement. Once a predetermined number of particles was placed to satisfy the target porosity of 0.36, the particles were enlarged to their final sizes, filling the allocated volume.

PFC3D models containing large number of particles are computationally-intensive. The minimum and the maximum values for particle radii were determined such that PFC3D models provided appropriate model resolution and were computed within a reasonable period of time. No attempt was made to match particle size to specimen’s grain size. On average, a numerical specimen contained 30 spherical particles across its diameter prior to introduction of clumps. Introduction of clumps decreased the number of rigid particles to approximately 10-15. However, because clumps were broken apart by introduction of vein networks, veined specimens contained 10-20 rigid particles across their diameters. In PFC3D models, particles should be viewed as means to discretize the specimen.

Because of the overlaps created during particle radii expansion, the resulting particle assembly had a high level of internal hydrostatic stress due to forces generated at particle contacts. High inter-particle forces were undesirable because parallel bonds fractured once they were introduced into the specimen and the walls constraining the particle assembly were removed. The internal stress, therefore, needed to be reduced to a low value. This was accomplished by uniform, gradual, controlled decrease of diameter of all particles within the assembly. Once an assembly with a low isotropic stress was attained, reduction in the number of floating particles – ones with fewer than three contacts – followed. Elimination of floating particles was necessary for having a well-connected specimen (Itasca 2008a; Potyondy and Cundall 2004).
Table 8-1 summarizes parameters used to generate the initial BPM. Figure 8-12b illustrates the final particle assembly.

Table 8-1. Parameters used in the generation of the initial bonded particle model.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Minimum ball radius, $R_{min}$</td>
<td>0.75 mm</td>
</tr>
<tr>
<td>Maximum ball radius, $R_{max}$</td>
<td>1.25 mm</td>
</tr>
<tr>
<td>Max. / min. radius ratio, $R_{max}/R_{min}$</td>
<td>1.66</td>
</tr>
<tr>
<td>Target porosity</td>
<td>0.36</td>
</tr>
<tr>
<td>Initial number of balls</td>
<td>37,900</td>
</tr>
</tbody>
</table>

Figure 8-12. Illustrations of walls that defined the specimen volume (a) and the final particle assembly (b).
Assignment of microscale properties to the balls and ball contacts within the model was carried based on the procedures and parameters described in Itasca (2008a). The properties are listed in Table 8-2.

**Table 8-2. Parameters used to assign micro-properties to balls and contacts within the model.**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Symbol</th>
</tr>
</thead>
<tbody>
<tr>
<td>Specimen bulk density, kg/m³</td>
<td>$\rho$</td>
</tr>
<tr>
<td>Effective contact modulus, Pa</td>
<td>$E_c$</td>
</tr>
<tr>
<td>Contact normal / shear stiffness ratio</td>
<td>$K_{ratio}$</td>
</tr>
<tr>
<td>Contact friction coefficient</td>
<td>$\mu$</td>
</tr>
</tbody>
</table>

Density of each ball in the model was calculated based on the volume of the ball, the volume of the specimen, and the specified bulk density ($\rho$). Normal stiffness ($k_n$) for each contact was calculated based on the sum of ball radii ($L$), the specified modulus ($E_c$), and the minimum of the ball radii ($r$) using the relationship from Itasca (2008a):

$$k_n = \frac{\pi r^2 E_c}{L} \quad \text{(Eq. 8-22)}$$

Each contact shear stiffness was calculated as follows:

$$k_s = \frac{k_n}{K_{ratio}} \quad \text{(Eq. 8-23)}$$

Selection of the values for the microscopic scale properties of Table 8-2 was part of a calibration process. The calibration approach and its results are presented as part of the modelling results (Chapter 9).

**8.4.4.2 Generation of Clumps**

Clumps were generated based on the approach described by Cho et al. (2007). Cho et al. (2007) initially created a PFC2D model with disks-shaped particles. The particles were then grouped together to form clumps. The grouping was performed by using an approach that Cho et al. (2007) called the *stamp* logic (Figure 8-13), which the authors described as follows:
“Using this logic, a clump can be created by stamping a circled area that corresponds to the desired grain size so that the particles within this area, if their center position is inside the stamped boundary, they can be added to a clump and grouped particles in a clump represent a grain acting as a single particle. The size of a clump is determined by specifying the radius of the stamp circle with a standard deviation and clump stamping is continuously activated until the 99% of the particles in the assembly are clumped.”

Figure 8-13. Illustration of the clumping logic used by Cho et al. (2007) in PFC2D models (after Cho et al. 2007).

The stamp logic of Cho et al. (2007) was implemented in PFC3D using a set of custom-built functions. A sphere rather than a circle was used to identify particles belonging to a clump. The centre of the stamp during the execution of the clumping algorithm was determined randomly within the specimen’s geometry. The algorithm was executed 37,900 times, which was equal to the number of balls in the initial BPM. Figure 8-14 illustrates the numerical specimen after the clump generation stage. Table 8-3 summarizes parameters used for clump generation and some metrics of the clumped BPM model.
Figure 8-14. Illustrations of the clumped specimen shown as an isometric view (a), in cross-section (b), and with details of clump geometry (c). Single particles are shown in white, and clumps are individually coloured.

Table 8-3. Summary of parameters used for clump generation and resulting characteristics of the clumped BPM model.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Iterations, no. of balls</td>
<td>37,900</td>
</tr>
<tr>
<td>Clump generation stamp radius</td>
<td>2.5 mm</td>
</tr>
<tr>
<td>Number of balls assigned to clumps (% of original)</td>
<td>32,819 (87%)</td>
</tr>
<tr>
<td>Number of balls after clump generation (% of original)</td>
<td>5,081 (13%)</td>
</tr>
<tr>
<td>Number of clumps</td>
<td>6,183</td>
</tr>
<tr>
<td>Minimum number of pebbles per clump</td>
<td>2</td>
</tr>
<tr>
<td>Maximum number of pebbles per clump</td>
<td>15</td>
</tr>
<tr>
<td>Parameter</td>
<td>Value</td>
</tr>
<tr>
<td>--------------------------------------------------</td>
<td>---------------</td>
</tr>
<tr>
<td>Average number of pebbles per clump</td>
<td>5</td>
</tr>
<tr>
<td>Standard deviation of number of pebbles per clump</td>
<td>3</td>
</tr>
<tr>
<td>Minimum clump volume</td>
<td>1.70 mm³</td>
</tr>
<tr>
<td>Maximum clump volume</td>
<td>8.18 mm³</td>
</tr>
<tr>
<td>Average clump volume</td>
<td>4.68 mm³</td>
</tr>
<tr>
<td>Standard deviation of clump volume</td>
<td>1.75 mm³</td>
</tr>
</tbody>
</table>

### 8.4.4.3 Bonding

In the final step of the intact specimen generation process, parallel bonds were introduced for all contacts between particles and clumps. Table 8-4 summarizes a set of microscopic scale parameters used to assign properties to the parallel bonds.

#### Table 8-4. Parameters used to assign micro-properties to parallel bonds within the model.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Symbol</th>
</tr>
</thead>
<tbody>
<tr>
<td>Effective modulus, Pa</td>
<td>$\tilde{E}_c$</td>
</tr>
<tr>
<td>Normal / shear contact stiffness ratio</td>
<td>$\tilde{K}_{ratio}$</td>
</tr>
<tr>
<td>Radius multiplier</td>
<td>$\tilde{\lambda}$</td>
</tr>
<tr>
<td>Friction angle, degrees</td>
<td>$\tilde{\phi}$</td>
</tr>
<tr>
<td>Tensile strength (mean and st. dev.), Pa</td>
<td>$\tilde{\sigma}_c$</td>
</tr>
<tr>
<td>Cohesion (mean and st. dev.), Pa</td>
<td>$\tilde{c}$</td>
</tr>
<tr>
<td>Moment contribution factor</td>
<td>$\tilde{\beta}$</td>
</tr>
</tbody>
</table>

Installation of a parallel bond and assignment of microscale properties to the bond were carried out individually for each contact in the model. A global contact list was used to cycle through all contacts in the model. The bond normal stiffness ($\tilde{k}_n$) was calculated by PFC3D using the relationship from Itasca (2008a):

$$\tilde{k}_n = \frac{\tilde{E}_c}{L}$$  \hspace{1cm} (Eq. 8-24)

The contact shear stiffness was calculated as follows:

$$\tilde{k}_s = \frac{\tilde{k}_n}{\tilde{K}_{ratio}}$$ \hspace{1cm} (Eq. 8-25)
Tensile strength and cohesion values for each individual parallel bond were calculated based on normal distributions generated using the supplied mean and standard deviation parameters. The same friction angle value was assigned to all parallel bonds.

Selection of the values for the microscopic scale properties of Table 8-4 was part of a calibration process. The calibration approach and its results are presented as part of the modelling results (Chapter 9).

8.4.5 Construction of Intact Veined Rock Numerical Specimen

Construction of a veined specimen was a two-step process. When a DVN is imported, the clumps intersected by veins are split into sub-clumps and/or single particles and smooth-joint contacts are introduced at the vein locations.

The clump splitting was necessary in modelling of veined rock. It eliminated potential formation of rigid particle bridges across veins. This is illustrated in Figure 8-15. In a non-clumped BPM, cross-vein bridges do not form (Figure 8-15, top row). In a clumped BPM, if clumps are not split, a potential exists for rigid cross-vein particle bridges to form (Figure 8-15, middle row) because clumps are rigid. Clump splitting eliminates this issue (Figure 8-15, bottom row).

Once clumps were split, parallel bond contacts at vein locations were substituted with smooth joint contacts. When the substitution occurs, the smooth joint contact normal and shear stiffnesses are inherited from the pre-existing parallel bond contacts as follows:

\[ k_{n,s} = \bar{k}_{n,s} + \frac{k_{n,s}}{A} \]  
(Eq. 8-26)

where \( k_{n,s} \) represents the normal/shear stiffness of the linear component of the parallel bond and \( A \) is the contact area. The value of the friction coefficient of the parallel bond is automatically assigned to the friction coefficient of the smooth joint.

Following the contact model substitution, microscopic properties were assigned to smooth joint contacts. The properties are summarized in Table 8-5.
Figure 8-15. Illustration of clump splitting. SJ refers to smooth joint contacts that are installed at contacts intersected by vein of the imported vein network.

Table 8-5. Parameters used to assign micro-properties to smooth joints within the model.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Symbol</th>
</tr>
</thead>
<tbody>
<tr>
<td>Normal stiffness scale factor</td>
<td>( \omega )</td>
</tr>
<tr>
<td>Contact normal / shear stiffness ratio</td>
<td>( K_{ratio} )</td>
</tr>
<tr>
<td>Friction coefficient</td>
<td>( \mu )</td>
</tr>
<tr>
<td>Radius multiplier</td>
<td>( \lambda )</td>
</tr>
<tr>
<td>Friction angle, degrees</td>
<td>( \phi )</td>
</tr>
<tr>
<td>Tensile strength, Pa</td>
<td>( \sigma_c )</td>
</tr>
<tr>
<td>Cohesion, Pa</td>
<td>( c )</td>
</tr>
<tr>
<td>Dilation angle</td>
<td>( \psi )</td>
</tr>
</tbody>
</table>
The majority of the micro-scale properties in Table 8-5 correspond to the properties of the smooth joint contact model (Section 8.4.3.3). The inherited value of the smooth joint contact normal stiffness ($k_n$) was scaled by the factor $\omega$. The value of the shear stiffness was calculated from the normal stiffness and $K_{ratio}$.

Selection of the values for the microscopic scale properties of Table 8-5 was part of a calibration process. The calibration approach and its results are presented as part of the modelling results (Chapter 9).

### 8.4.6 Specimen Loading

The axial compressive loading to a specimen was applied using internal loading, a technique described by Itasca (2008a). The internal loading involved application of strain by applying axial velocities to all particles within the model as well as to platens. Once an increment of strain had been applied, the axial velocities of particles were reset and the platens were stopped. The material was then allowed to reach mechanical equilibrium after which a new strain increment was applied (Itasca 2008a).

Confining pressure was applied using the jacket wall used in sample construction. The confinement was controlled by expanding and contracting the jacket. The model implemented a feedback mechanism which allowed the model to maintain assigned confining pressure during confined tests. The jacket was removed in unconfined tests.

In numerical models simulating direct tension experiments, the loading was carried out by applying constant opposite velocities to the particles located within 5 mm of each end of the specimen. These particles served as grips, allowing the specimen to be slowly pulled apart.

### 8.4.7 Stress-Strain Measurement Techniques

In compression experiments, the axial strain was calculated based on platen positions. In direct tension experiments, the axial strain was calculated based on the positions of gauge balls. The gauge balls were model particles located at the periphery of the specimen. Paired axial gauge balls were positioned directly under the loading platens, along the specimen’s long axis.

In triaxial compression experiments, the diametric strain was calculated based on the varying diameter of the jacket. In the absence of a jacket, as in the case of an unconfined test,
gauge balls were used to keep track of the diametric strain. Two pairs of lateral gauge balls were located midpoint of the specimen’s height, at the circumference of the specimen.

Stresses in compression experiments were calculated by dividing the wall forces by the area of the specimen. The platen forces were used to calculate the axial stress, and forces on the jacket (when present) for calculation of the confining pressure. In direct tension tests, the axial stress was calculated based on the forces developed at the contacts between the particles representing the grips and the adjacent particles of the specimen.

8.5 BPM Limitations in Modelling of Rock Behaviour

Bonded particle modelling is a popular approach for studying behaviour of rock. The technique, however, has a number of limitations as many researchers (e.g. Cho et al. 2007; Potyondy and Cundall 2004; Potyondy 2011; Turichshev and Hadjigeorgiou 2016a) pointed out. Most notable limitations of bonded particle models include:

- **Particle size dependency**: In addition to model resolution, the particle size in a BPM model controls the material fracture toughness (Potyondy and Cundall 2004). Consequently, models calibrated based on one particle size require extensive re-calibration when the particle size changes.

- **Low compressive-to-tensile strength ratio**: A parallel-bonded model that is calibrated to match the uniaxial compressive strength of a typical hard rock will have its simulated tensile strength being much higher than of the real rock (Cho et al. 2007; Diederichs 2000). In general, a parallel-bonded model can be calibrated to match either unconfined compressive strength or tensile strength of rock but not both.

- **Linear failure envelope**: The failure envelope generated based on a parallel-bonded model tends to be linear unlike a non-linear failure envelope for hard rock (Cho et al. 2007).

- **Low material friction angle**: The failure envelope of a BPM material normally exhibits a friction angle that is lower in comparison to friction angle values established based on laboratory experiments for hard rock (Cho et al. 2007; Potyondy and Cundall 2004). In effect, BPMs do not display the same strength gains with the application of confinement as hard rock.

It has been shown (e.g. Cho et al. 2007; Potyondy 2011) that some of the limitations of BPMs are related to the use of parallel bonds and to poor particle interlocking due to their circular
or spherical shape. Once standard parallel bonds break, particles are not able to resist further moments and displacements as they simply rotate excessively, rendering friction developed between them insufficient (Potyondy 2011). Consequently, the BPM materials often cannot develop the desired angle of internal friction to match the rock properties.

In order to overcome some of the limitations associated with the use of parallel bonds, Potyondy (2011) developed enhanced parallel bonds. An enhanced parallel bond is a standard parallel bond that is formulated such that moment contributions to bond stress are reduced or completely ignored in the bond failure criterion (Potyondy 2011). The moment contribution is controlled through the $\beta$ parameter (see Equations 6-13 and 6-14 in Section 8.4.3.2).

Potyondy (2011) successfully used the enhanced parallel bond approach to reproduce the tensile and compressive strengths of Åspro diorite. The models, however, demonstrated a premature material softening. Laboratory experiments on Åspro diorite showed that the pre-peak portion of the stress-strain curve was largely linear until peak. Numerical experiments of Potyondy (2011) with enhanced parallel bonds, however, showed significant pre-peak modulus softening following the crack initiation stress. Potyondy (2011) states that the use of models with enhanced parallel bonds, where the moment contribution to the bond failure is disregarded, is questionable.

Cho et al. (2007) investigated the use of clumped particles for modelling of intact rock in two dimensions. In particular, they looked whether clumped particle models can be used to eliminate some of the shortcomings associated with the use of standard BPMs. Cho et al. (2007) concluded that the approach they had developed was promising. The authors were able to reproduce the strength envelope of the Lac Du Bonnet granite.

Given the success of Cho et al. (2007) in using clumped particles to model intact rock, bonded particle models for intact veined rock were developed based on clumps. The approach was adequate for reproducing the target strength envelope (Chapter 9), but the attained compressive-to-tensile strength ratio was still low. The experiments with various confining pressures were modelled using the bonded block modelling approach in order to capitalize on potential improvements of the method over the bonded particle models.
8.6 Bonded Block Models

It has been shown that rock can be modelled using multi-faceted bonded blocks in place of circular/spherical particles. Damjanac et al. (2007), Gao (2013), Lan et al. (2010), Nicksiar and Martin (2014) used grain based models (GBM) to model intact rock in 2D using a discrete element software. In 3D, Garza-Cruz et al. (2014) used bonded tetrahedral blocks to create SRM-type models to replicate rock spalling in underground drifts.

Lan et al. (2010) modeled compression experiments on Lac du Bonnet granite and Åsprö diorite using the Universal Distinct Element Code (UDEC). In their 2D models, specimens were discretized into polygonal blocks (grains) based on Voronoi tessellation diagrams. Lan et al. (2010) attained good results in calibrating numerical models to laboratory experiments, demonstrating advantages of using the GBM over the BPM approach.

Gao (2013) used triangular blocks in 2D (UDEC) and tetrahedral blocks in 3D (3DEC code) models to simulate compressive and Brazilian tests of coal specimens. His work showed that bonded block models could reproduce various fracture patterns commonly observed in laboratory experiments. Gao (2013) also demonstrated that GBMs exhibited key characteristics found in intact rock under compression, such as crack initiation and crack damage thresholds.

8.7 3DEC Modelling

8.7.1 Background

Similar to PFC3D, 3DEC is a three-dimensional numerical software package based on the distinct element method (Itasca 2013). 3DEC can simulate the response of discontinuous media that is subjected to either static or dynamic loading. The discontinuous medium is represented as an assemblage of discrete polyhedral blocks bound by discontinuities. The blocks can be of two types (Itasca 2013):

1. Rigid, with three translational and three rotational degrees of freedom (DOF) or
2. Deformable, which are internally subdivided into tetrahedral discrete element zones, with each zone having three translational DOFs at each vertex termed node.

Faces of rigid blocks are planar polygons. Deformable blocks have faces that are discretized into triangular sub-faces. A 3DEC model cannot contain a mixture of rigid and deformable blocks; all blocks in the model have to be of the same type (Itasca 2013).
Each element (zone) within a deformable block in 3DEC responds according to a prescribed linear or nonlinear stress-strain law (Itasca 2013). 3DEC has several built-in models that describe behavior of intact material.

Discontinuities define boundaries between blocks. A contact is formed when two faces of two blocks touch. Each contact is discretized into sub-contacts. Interaction forces are applied at sub-contacts. The sub-contacts are also responsible for keeping track of other contact conditions such as sliding and separation (Itasca 2013). The mechanical behaviour of discontinuities is prescribed based on integrated models.

8.7.2 Block Constitutive Behaviour

3DEC numerical specimens were built using tetrahedral deformable blocks. Blocks were discretized (zoned) into tetrahedral zones. Use of deformable blocks allowed to bring elements of continuum modelling into a discrete element model. With rigid blocks, strain and stresses are only allowed to develop at block contacts. Having deformable blocks allowed strain and stresses to develop inside the blocks in addition to the block contacts.

Zones within the model blocks were assumed to have an elastic isotropic behaviour. The elastic isotropic constitutive model in 3DEC provides linear stress-strain behavior with no hysteresis on unloading (Itasca 2013).

The elastic isotropic model was chosen because it does not contain a predetermined mechanism for material to develop failure. This was consistent with the SRM methodology allowing the rock behaviour to emerge during the simulation and not to be prescribed beforehand. The model was also simple, relying only on three parameters: density, Young’s modulus, and Poisson’s ratio.

8.7.3 Contact Constitutive Behaviour

The Coulomb-slip model was used for the contacts between the blocks. This is the simplest 3DEC discontinuity model that allows for contact failure (Itasca 2013). Figure 8-16 illustrates the force-displacement law of the Coulomb-slip contact model.
Figure 8-16. Force-displacement relationships for the Coulomb-slip model: a) normal force vs. normal displacement, b) shear force vs. shear displacement, and c) effect of dilation on the normal displacement.

In the elastic range, the contact behaviour is governed by the normal ($K_n$) and shear ($K_s$) stiffness parameters (Itasca 2013):

$$\Delta F^n = -K_n \Delta U^n A_c$$  \hspace{1cm} (Eq. 8-27)

$$\Delta F^s_i = -K_s \Delta U^s_i A_c$$  \hspace{1cm} (Eq. 8-28)

where $\Delta F^{n(s)}$ is incremental normal (n) or shear (s) force, $\Delta U^{n(s)}$ is incremental normal (n) or shear (s) displacement, $A_c$ is subcontact area, and $i$ subscript designates a vector.

A contact does not fail in pure compression. In tension, while it is intact (no prior slip or separation), the contact’s maximum tensile force ($T_{max}$) is limited by its tensile strength, $\sigma_{tmax}$ (Itasca 2013):

$$T_{max} = -\sigma_{tmax} A_c$$  \hspace{1cm} (Eq. 8-29)
The contact breaks in tension once the tensile strength is exceeded. Upon tensile failure, the contact’s normal and shear forces are set to zero.

The maximum shear force \( F_{s_{\text{max}}} \) that a contact can sustain without slipping is governed by Coulomb’s friction law (Itasca 2013):

\[
F_{s_{\text{max}}} = c \ A_c + F^n \ \tan \varphi
\]  
(Eq. 8-30)

where \( c \) and \( \varphi \) are respectively cohesion and friction angle.

Equation 6.25 can be rewritten in terms of the maximum shear \( \tau_{\text{max}} \) and normal \( \sigma_n \) stresses as follows:

\[
\tau_{\text{max}} = c + \sigma_n \ \tan \varphi
\]  
(Eq. 8-31)

Contact slip occurs once the maximum shear force is reached. At this point, the contact’s tensile strength, cohesion, and friction angle are reduced to their respective residual values.

Dilation of the contact occurs only when the contact is in slip state. The incremental shear displacement \( \Delta U^s \) generates an incremental normal displacement due to dilation \( \Delta U^n_{\text{dil}} \) according to the following relationship (Itasca 2013):

\[
\Delta U^n_{\text{dil}} = \Delta U^s \ \tan \psi
\]  
(Eq. 8-32)

where \( \psi \) is a dilation angle.

The normal force of the contact is corrected as follows (Itasca 2013):

\[
F^n = F^n + K_n \Delta U^n_{\text{dil}} \ A_c
\]  
(Eq. 8-33)

Dilation angle is set to zero once the magnitude of shear displacement exceeds \( U_{\text{lim}}^s \).

8.7.4 Construction of Intact Rock Specimen

Construction of an intact specimen geometry began with creation of a block model. A block model is a collection of tetrahedral blocks in 3DEC. The final specimen geometry was created by trimming the block model to a cylinder and adding top and bottom platens and a jacket around the assembly.
8.7.4.1 Initial Block Model

A block model, consisting of a set of tetrahedral blocks, was created outside of 3DEC. A combination of Rinoceros3D software (by Robert McNeel & Associates), a 3D computer aided design (CAD) package, and Kubrix software (by Itasca), a 3D meshing application, was used to create an initial block assembly.

The block assembly was built larger than the desired size of the specimen. In Rhinoceros3D, a mesh of a 135 mm high by 60 mm diameter cylinder was created. Using Kubrix, the volume inside the mesh was discretized into tetrahedral blocks with 5 mm long edges. Figure 8-17 illustrates the initial block model. The resulting block model used in numerical experiments was composed of 24,576 distinct tetrahedral blocks.

![Figure 8-17](image.png)

Figure 8-17. An isometric view of the block model (a) used as a source for generation of numerical specimens for intact rock modelling in 3DEC. A vertical cross-sectional view (b) shows the internal arrangement of blocks within the block model. Both images are coloured by individual blocks.
The tetrahedral blocks in the model did not intend to represent individual grains of rock specimens. Representation of individual grains in a 3D DEM model is not possible at this time due to limitations of model size and time required to complete computer simulations. Such models would need to be composed of millions of blocks, requiring excessive memory and computational resources, likely running for several months to reach completion. In this project, blocks were only elements in the model discretization. The edge length of 5 mm was selected based on a number of trials. Models generated with block edges of 5 mm were found to produce good model resolution and able to compute in reasonable time.

8.7.4.2 Model Geometry

Preparations of the final model geometry, based on the block model, consisted of three steps. First, the block model was trimmed to the size of the specimen. Second, the top and the bottom platens were created. Last, the jacket around the resulting assembly was built. Figure 8-18 illustrates the final assembly.

Figure 8-18. Finished specimen geometry with platens is shown in (a). An isometric view model is shown in (b), and a section through the model is shown in (c). The platens and the jacket had a thickness of 5 mm.
The trimming was first carried out by removing 2.5 mm from the top and the bottom edges of the block model, bringing the specimen height to the target value of 125 mm. The top and the bottom platens were created as individual prismatic blocks 5 mm thick, with a 60 x 60 mm square base, positioned above and below the specimen. Circumferential trimming of the assembly was then carried out using vertical cuts initiated 25 mm away from the centre of the block model. This created a 50 mm diameter specimen. Following the trimming, the number of blocks representing the specimen was reduced to 16,494 from the original 24,576.

The jacket around the specimen/platen blocks was constructed as a series of prismatic blocks. The number of blocks corresponded to the number of vertical cuts used to trim the specimen and the platens. Like the platens, the jacket was made 5 mm thick.

8.7.4.3 Zoning

After the model geometry was built, all blocks were made deformable. This was done automatically in 3DEC by subdividing each block into zones.

In 3DEC, the size of zones is defined by specifying the average edge length of tetrahedral zones. As the number of zones influences the size and the run time of the model – more zones result in a larger model that takes longer time to solve – each block representing a rock specimen was aimed to contain a single zone. A zone edge length of 10 mm was used. Blocks representing the platens and the jacket were zoned to contain more than a single zone per block.

8.7.5 Construction of Intact Veined Rock Numerical Specimen

Construction of a 3DEC model for an intact veined specimen was similar to building the model for the intact specimen. In the case of a veined specimen, a source block model was built to contain a vein geometry. The remaining steps of the process, including block model trimming, construction of the platens and the jacket, and zoning, were identical.

A block model for each intact veined specimen was created using the specimen’s discrete vein network (Chapter 4). The process is illustrated in Figure 8-19. As the first step, a 55 x 55 x 130 mm prismatic model of blocks, defined by vein geometry, was constructed using the Rinoceros3D software. Kubrix was then used to generate a block model. Block edges were specified being 5 mm in length, identical to the intact specimen.
Figure 8-19. Illustration of stages involved in generating an intact veined specimen for a 3DEC model.

8.7.6 Property Assignment

Assignment of material properties was the last step in the specimen generation process. Properties for the blocks and contacts were assigned independently. Blocks were divided into 3 categories, either belonging to the specimen, the platens, or the jacket. Table 8-6 lists the parameters that were used to specify material properties for blocks in 3DEC models. These were directly assigned to zones within the models.

Table 8-6. Parameters used to assign properties to zones within 3DEC model.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Symbol</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density, kg/m³</td>
<td>( \rho )</td>
</tr>
<tr>
<td>Young’s modulus, Pa</td>
<td>( E_c )</td>
</tr>
<tr>
<td>Poisson’s ratio</td>
<td>( \nu )</td>
</tr>
</tbody>
</table>

Contacts were divided into 5 potential categories based on the types of blocks that form the contact: internal rock, rock vein, rock-platen, rock-jacket, and platen-jacket. Vein-type contacts were not present in the models of intact material; only internal rock contacts existed. In the models of veined rock both vein and internal contacts were present. Vein contacts represented boundaries of intact rock fragments, and internal contacts joined individual blocks inside of an intact rock
Table 8-7 summarizes microscopic scale parameters for the Coulomb-slip model used to define the behaviour of contacts between blocks.

**Table 8-7. Parameters used to assign properties to block contact within 3DEC model.**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Symbol</th>
</tr>
</thead>
<tbody>
<tr>
<td>Normal stiffness, Pa/m</td>
<td>$k_n$</td>
</tr>
<tr>
<td>Normal / shear stiffness ratio</td>
<td>$K_{rat}$</td>
</tr>
<tr>
<td>Tensile strength (mean, st. dev., min, and max), Pa</td>
<td>$\sigma_{tmax}$</td>
</tr>
<tr>
<td>Cohesion / tensile strength ratio</td>
<td>$c_{ratio}$</td>
</tr>
<tr>
<td>Friction angle (mean, st. dev., min, and max), degrees</td>
<td>$\phi$</td>
</tr>
<tr>
<td>Residual tensile strength, Pa</td>
<td>$\sigma_{tr}$</td>
</tr>
<tr>
<td>Residual friction angle, degrees</td>
<td>$\phi_{tr}$</td>
</tr>
<tr>
<td>Dilation angle, degrees</td>
<td>$\psi$</td>
</tr>
<tr>
<td>Displacement for zero dilation, m</td>
<td>$U_{lim}$</td>
</tr>
</tbody>
</table>

Assignment of microscale properties to block contacts was carried out individually for each contact in the model. A global contact list was used to cycle through all contacts in the model. The contact shear stiffness was calculated as follows:

$$k_s = \frac{k_s}{K_{rat}}$$  \hspace{1cm} (Eq. 8-34)

Tensile strength and friction angle for each individual contact were calculated based on a normal distributions generated using the supplied mean and standard deviation parameters, bound by the minimum and maximum values (Table 8-7). The contact cohesion value was calculated based on the following relationship:

$$c = \frac{\sigma_{tmax}}{c_{ratio}}$$  \hspace{1cm} (Eq. 8-35)

Selection of the values for the microscopic scale properties listed in Table 8-7 was part of a calibration process. The calibration approach and its results are presented as part of the modelling results (Chapter 9).
8.7.7 Testing

The jacket blocks were deleted from the model for direct tension or unconfined compression experiments. For direct tension experiment, the properties of the contacts between the specimen and the platens were modified. In order to prevent de-bonding between the specimen and the platens, the contacts were assigned high tensile and shear strength values.

The axial loading of the specimen was carried out in the displacement control mode, with constant final platen velocities. Velocities were applied to both platens simultaneously. Downward velocity was applied to the top boundary of the upper platen, and upward velocity was applied to the bottom boundary of the lower platen. To minimise the potential disturbance to the model due to the velocity application, the platens were slowly accelerated until the final velocity was attained. Once the velocity of the platens reached the specified value, it remained constant until the experiment was complete.

In a triaxial experiment, specimens were initially loaded hydrostatically. Once the target confining pressure was attained, axial loading of the specimen continued and application of the confining pressure remained constant.

In the numerical models, the confining pressure was applied to the specimen using the jacket. The confinement was generated by “squeezing” the jacket around the specimen. The squeezing was achieved by applying velocities to the outside boundary of the jacket, directed towards the specimen. Velocities were proportional to the confinement demand at every step of the experiment.

8.7.8 Monitored Test Parameters

The key parameters measured during each experiment included axial strain, lateral strain, axial stress, and confining pressure (in triaxial experiments only). Measurements were carried out once per 100 model cycles.

8.7.8.1 Strain

Three pairs of grid points (zone vertices) were identified prior to specimen loading. Two grid points, each closest to the centre at each end of the specimen, were used to monitor specimen’s axial strains. The separation distance between these grid points prior to the experiment defined the
initial specimen length. Once the experiment began, the changes in the grid point separation distance were monitored, and the axial strain was calculated.

Lateral strain was monitored in a similar manner. Two pairs of grid points, one along the x-axis and one along the y-axis of the specimen, were identified. Initial specimen dimensions were recorded prior to the experiment, and changes in the separation distances were used to calculate lateral strains in x and y directions. The experimental lateral strain was calculated by averaging the two measurements.

8.7.8.2 Stress

Axial stress during the experiment was calculated based on the forces developed at the contacts between the specimen and the top and the bottom platens. Normal forces in the contacts at the top platen were summed and divided by the original area of the specimen, resulting in calculation of stress at the top platen. The stress at the bottom platen was calculated similarly. The axial experimental stress was taken as an average of the two stress values. This approach made calculation of axial stress identical to the method used in the laboratory experiments.

The confining pressure during the experiment was calculated based on the forces developed in the contacts between the specimen and the jacket. The sum of normal forces was divided by the initial side area of the specimen, resulting in the confining pressure. The calculation of confining pressure, based on the outside contact forces, closely resembled laboratory experiments in which the oil pressure used to apply the confinement was monitored.

8.8 Conclusions

The main objectives of this chapter were to introduce the SRM modelling approach, to provide necessary background into the mechanics of the technique, and to describe how numerical models were developed in this research to model the experiments on intact veined rock.

The synthetic rock mass numerical modelling approach was selected to model the laboratory triaxial compression experiments on the intact veined rock specimens. The SRM appeared being most suitable for the task. The method allowed for explicit representation of both intact rock, which could fracture during an experiment, and of discontinuities that defined blocks of intact material.
Analysis of the traditional, based on bonded particle models, SRM modelling implementation (Mas Ivars et al. 2011), demonstrated that the BPM modelling technique possessed necessary capabilities for modelling of intact veined rock. BPM models employed a sophisticated particle-bonding mechanism which allowed the particulate material to behave similar to a solid and to fracture when forces or moments exceeded the bond strengths. Veins in BPM models could be represented with smooth joint contacts which allowed to eliminate contact “bumpiness”. The initial experiments carried out using the “caving” stress path were modeled based on the traditional SRM approach.

Parallel-bonded particle models, unfortunately, suffered from several limitations, one of which was having difficulties in reproduction of target tensile and compressive strengths by the same model. The limitations of BPMs had been recognized during modelling. As some of the BMP limitations were related to the spherical shape of the particles, use of SRM models based on tetrahedral bonded blocks was investigated. Bonded block models were used to model the standard triaxial experiments.

By proving key information on the theory behind the numerical simulations, the simulation results can be discussed. This is the topic on the next chapter of the thesis. Chapter 9 discusses the BPM and BBM modelling of the laboratory experiments.
Chapter 9
Results of SRM Modelling of the Triaxial Experiments on the Intact Veined Rock Specimens

9.1 Introduction

This chapter is organized into two main sections, each describing the results of the simulations for the two sets of experiments. For each modelling approach, calibration of the intact material is described first. Results of the modelling of the laboratory experiments (described in Chapter 6 and Chapter 7) follow. A discussion section for each modelling approach is provided, aimed to address key findings of the simulations. Conclusions based on each simulation approach are given at the end of each main section. Summarizing remarks are provided at the end of the chapter.

Chapter 8 described the synthetic rock mass modelling framework and its application to modelling of intact veined specimens. Numerical simulation approaches relying on the use of the discrete element method based on Bonded Particle Models (BPM) with the PFC3D software and Bonded Block Models (BBM) with the 3DEC application were employed to simulate respectively the experiments discussed in Chapter 6 and Chapter 7. This chapter presents the results of these numerical simulations. Preliminary results of BPM simulations were reported by Turichshev and Hadjigeorgiou (2015).

The main objectives of the numerical simulations included:

- Investigation of the applicability of the SRM numerical modelling technique for modelling of intact veined rock in compression in 3D.
- Comparison of BPM and BBM based modelling approaches.
- Development of further insight into the behaviour of intact veined rock.

9.2 Modelling of “Caving” Stress Path Triaxial Experiments with Bonded Particle Models

Use of the bonded particles/clumps and SRM required two calibration stages for numerical models. The first stage involved calibration of micro-scale bond properties of the material representing the intact portion of the numerical specimen. In the second stage, micro-scale properties of the contacts representing mineral veins were calibrated.
9.2.1 Calibration of Intact BPM Material

In calibration of intact material behaviour, particle/clump micro-properties were selected such that the Young’s modulus, Poisson’s ratio, and peak axial strength of the intact numerical specimen attained the best match with the target values obtained from laboratory experiments. Table 6-1 summarizes the target parameters of intact rock.

**Table 9-1. Target parameters used for calibration of intact specimen.**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Young’s modulus (GPa)</td>
<td>50</td>
</tr>
<tr>
<td>Poisson’s ratio</td>
<td>0.28</td>
</tr>
<tr>
<td>Unconfined compressive strength (MPa)</td>
<td>125</td>
</tr>
<tr>
<td>Tensile strength (MPa)</td>
<td>14</td>
</tr>
</tbody>
</table>

The adopted calibration approach for intact material followed the one described by Potyondy and Cundall (2004). The target macroscopic stiffness of the specimen was attained by adjusting the micro-scale particle contact and parallel bond moduli. In order to simplify the calibration process, the values of the moduli were set equal to each other ($E_c = \bar{E}_c$). By making adjustments to the normal-to-shear stiffness ratios of the particle contacts and parallel bonds, which were also set equal to each other ($K_{ratio} = \bar{K}_{ratio}$), the target macroscopic Poisson’s ratio was attained. Adjustments to the parallel bond tensile strength and cohesion were used to match the target unconfined compressive strength. Table 9-2 summarizes a set of the micro-scale parameters that allowed for a clumped BPM specimen of intact material to attain the target macro-scale response specified in Table 6-1. The micro-scale parameters of Table 9-2 were explained in detail in Chapter 8.

**Table 9-2. Parameters used in the generation of a calibrated PFC3D model of intact rock.**

Explanation of the parameters can be found in Chapter 8.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Bulk density, $\rho$ (kg/m³)</td>
<td>2,750</td>
</tr>
<tr>
<td>Modulus, $E_c$ (GPa)</td>
<td>29</td>
</tr>
<tr>
<td>Normal / shear stiffness ratio, $K_{ratio}$</td>
<td>7</td>
</tr>
</tbody>
</table>
Table 9-3 lists the macro-scale material properties used for model calibration and the values of the same parameters attained by a calibrated intact numerical specimen. The calibrated numerical specimen replicated the elastic parameters and the UCS value. The tensile strength, however, was overestimated in the numerical specimen. This was expected as BPM models calibrated to match compressive strength tend to overestimate the tensile strength (see Chapter 8).

Table 9-3. Comparison between target and calibrated macro-properties of the PFC3D model.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Target Value</th>
<th>PFC3D</th>
</tr>
</thead>
<tbody>
<tr>
<td>Young’s modulus (GPa)</td>
<td>50</td>
<td>50</td>
</tr>
<tr>
<td>Poisson’s ratio</td>
<td>0.28</td>
<td>0.28</td>
</tr>
<tr>
<td>Unconfined compressive strength (MPa)</td>
<td>125</td>
<td>128</td>
</tr>
<tr>
<td>Tensile strength (MPa)</td>
<td>14</td>
<td>22</td>
</tr>
</tbody>
</table>

Figure 9-1 shows the stress-strain responses of the calibrated intact clumped BMP specimen subjected to the direct tension, UCS, and triaxial (confining pressure of 12 MPa) numerical experiments. As the laboratory compression tests were carried out under the confining pressure of 12 MPa, a 12 MPa triaxial numerical experiment was carried out to assess the specimen’s strength under similar conditions. In laboratory, the specimens attained the maximum
strength of 202.5 MPa. The calibrated numerical specimen of intact material attained the strength of 212 MPa under confining pressure of 12 MPa.

![Graphs showing stress-strain and strength envelope](image)

**Figure 9-1. Results of a direct tension test (a), compression tests (b), and the corresponding Hoek-Brown strength envelope (c) for a clumped BPM model of intact material.**

In part c) of Figure 9-1, results of the tensile and compressive test simulations are plotted in the principal stress space. A strength envelope (red line) was fitted through the data points based on the approach described by Hoek and Brown (1997). The \( m_i \) is a statistical constant in the Hoek and Brown criterion for intact rock that is determined by statistical analysis of the results of a set of triaxial tests on core specimens. For the calibrated intact BPM material \( m_i \) was calculated to be 7.4. For CMET rock, Brzovic (2010) reported \( m_i \) value of 9.1.

### 9.2.2 Modelling of Veined Specimens

Modelling of intact veined rock specimens involved calibration of properties of vein contacts such that the model replicated key characteristics of the laboratory experiments on intact veined rock. This involved establishing micro-scale mechanical parameters of smooth joints representing veins. The key parameters included smooth joint contact stiffness and strengths.
Experimental data related to the mechanical behaviour and properties of mineral veins is scarce. Calibration of individual veins was not attempted in this work. Instead, results of the experiment on Specimen 1 were used to calibrate micro-scale properties of smooth joint contacts. The calibration results were then applied in modelling of the remaining specimens.

The results of the triaxial experiments on intact veined rock (Chapter 6 and Chapter 7) could be potentially used to estimate shear strengths of veins. This would involve computing normal and shear stresses acting on veins based on vein orientations, axial stress, and confining pressure at the time of failure (Parry 2004). This approach was not utilized in the calibration process because the underlying assumptions were believed being inapplicable for the experiments. First, the veins were not planar, and normal and shear stresses acting on them, therefore, were not uniform. Second, because specimens of intact veined rock were heterogeneous, the stress field within the specimens were not uniform. Stress orientations were also distorted near specimen ends where many fractures were observed. Last, because establishing vein shear strength characteristics was by calibration, selection of more appropriate initial values was not deemed to be critical.

### 9.2.2.1 Calibration Methodology

In order to achieve a reduction in macroscopic Young’s modulus from 50 GPa (intact material) to 26 GPa (average of the experiments of Chapter 6), the normal stiffness of contacts representing veins was gradually reduced until the target macroscopic Young’s modulus was reached. The same amount of softening was applied to all smooth joint contacts in the model. Possible individual vein characteristics were not considered.

The macroscopic peak strength was calibrated by finding a combination of smooth joint contact tensile and shear strengths. This was done by selecting a reasonable value of tensile strength and of friction angle and finding a value for cohesion of smooth joint contacts such that the triaxial strength attained by the specimen was matched by the model. Similar to stiffness assignment, individual vein characteristics were not considered in assignment of strength parameters; each smooth joint contact in the model was assigned an identical value.

### 9.2.2.2 Calibration of Specimen 1 Experiment

The calibration process for the experiment on Specimen 1 produced a set of smooth joint micro-scale properties that are summarized in Table 9-4. A tensile strength of 10 MPa and a friction angle of 45° were used for smooth joint contacts.
Table 9-4. Summary of calibrated smooth joint micro-scale parameters of Specimen 1 SRM model. Explanation of the parameters can be found in Chapter 8.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Normal stiffness scale factor, $\omega$</td>
<td>0.054</td>
</tr>
<tr>
<td>Ratio of normal to shear stiffness, $K_{ratio}$</td>
<td>0.7</td>
</tr>
<tr>
<td>Friction coefficient, $\mu$</td>
<td>0.5</td>
</tr>
<tr>
<td>Dilation angle, $\psi$ (degrees)</td>
<td>0</td>
</tr>
<tr>
<td>Radius multiplier, $\lambda$</td>
<td>1</td>
</tr>
<tr>
<td>Tensile strength, $\sigma_c$ (MPa)</td>
<td>10</td>
</tr>
<tr>
<td>Cohesion, $c$ (MPa)</td>
<td>63.7</td>
</tr>
<tr>
<td>Cohesion/tensile strength ratio, $c/\sigma_c$</td>
<td>6.4</td>
</tr>
<tr>
<td>Friction angle, $\phi$ (degrees)</td>
<td>45</td>
</tr>
</tbody>
</table>

Figure 9-2 provides a visual representation of the simulation results. A photograph of Specimen 1 is displayed in part a), illustrating the final fracture developed through the specimen. The SRM specimen is shown with the discrete vein network in part b) of the figure. Vectors indicating the displacements of particles and clumps are shown in part c). Part d) illustrates smooth joint contacts broken during the numerical simulation, with red disks representing contacts that fractured in tension and green in shear. Part e) of the figure illustrates axial stress-strain curves, one from the experiment and one obtained as the results of the numerical simulation, allowing side-by-side comparison of the experimental and numerical results.

The numerical SRM specimen fractured along smooth joint contacts representing vein #1, which had an orientation of 65° with respect to the diametric axis of the specimen. In the laboratory experiment, the fracturing occurred mostly along vein #3 and in part along vein #10, both oriented 62°, with an average orientation of the final fracture being 65°. The numerical model did not precisely reproduce the fracturing of the tested specimen but was able to demonstrate fracturing occurring on a vein at similar position and of similar orientation.
Figure 9-2. Illustration of a fracture developed in Specimen 1 (a) and in the corresponding SRM specimen (b), with displacement vectors shown in (c). Smooth-joint contacts broken in tension (red disks) and in shear (green disks) are shown in (d). Axial stress-strain diagrams for the specimen (black line) and its calibrated SRM model (red line) are shown in (e).

Brzovic (2010) observed that strength of CMET veins was related to their thickness: thin veins were generally stronger than thick veins. In Specimen 1, vein #3 had a thickness of 1.8 mm, vein #10 – 0.4 mm, and vein #1 – 0.9 mm. Given observations of Brzovic (2010), vein #3 (fractured in the experiment) had lower strength than vein #1 (numerical model fracture). In the numerical model, however, all smooth joint contacts representing veins had identical strength properties. The model was not setup to be able to capture the strength difference between the two veins.

From Figure 9-2d one can see that the fracturing of the smooth joint contacts was dominated by shear contact failures. This suggests that specimen strength was controlled by vein shear strength and that vein tensile strength had limited effects.

9.2.2.3 Modelling of Specimen 2 Experiment

Modelling of Specimen 2 was carried out using the values of smooth joint micro-scale parameters from the calibrated numerical model of the Specimen 1 experiment (Table 9-4). Figure
9-3 provides a visual representation of the numerical simulation results on the SRM model of Specimen 2.

The SRM model was able to partially replicate fracturing that Specimen 2 sustained during the experiment. The SRM specimen fractured along vein #1 (Figure 9-3a,b), just like in the laboratory experiment. The numerical model was not able to replicate partial fractures along veins #2 and #7.

![Figure 9-3. Illustration of a fracture developed in Specimen 2 (a) and in the corresponding SRM specimen (b), with displacement vectors shown in (c). Smooth-joint contacts broken in tension (red disks) and in shear (green disks) are shown in (d). Axial stress-strain diagrams for the specimen (black line) and its calibrated SRM model (red line) are shown in (e).](image)

The model was able to replicate the macroscopic Young’s modulus of Specimen 2. In the laboratory experiment, Specimen 2 attained a peak strength of 203 MPa. The SRM model only reached a peak strength of 191 MPa, which was identical to the strength of the model of Specimen 1. As discussed in Chapter 6, Specimen 2 experienced two stages of fracturing. Initially, the specimen fractured in part along veins #2 and #7. The model did not show fracturing along those veins. Following the initial fracture at 203 MPa axial stress, the stress fell but then recovered,
reaching a new local maximum of 190 MPa. The second fracturing episode occurred along vein #1, and the model was able to replicate this, as well as the maximum stress level associated with this event.

9.2.2.4 Modelling of Specimen 6 Experiment

Modelling of Specimen 6 was carried out using the values of smooth joint micro-scale parameters from the calibrated numerical model of the experiment on Specimen 1 (Table 9-4). Figure 9-4 provides a visual representation of the numerical simulation results from the SRM model of Specimen 6.

![Vein fractured in experiment](image)

Figure 9-4. Illustration of a fracture developed in Specimen 6 (a) and in the corresponding SRM specimen (b), with displacement vectors shown in (c). Smooth-joint contacts broken in tension (red disks) and in shear (green disks) are shown in (d). Axial stress-strain diagrams for the specimen (black line) and its calibrated SRM model (red line) are shown in (e).

The SRM model was able to replicate the fracture developed by Specimen 6 during the experiment. Both Specimen 6 and its numeric counterpart fractured along vein #2.

The SRM model was able to reproduce the elastic constants of Specimen 6. The Young’s modulus of 28 GPa was recorded in both the experiment and the SRM model. The Poisson’s ratio of 0.30 was measured in the experiment and the value of 0.31 was attained by the model.
The model was not able to reproduce the peak axial strength developed by Specimen 6. The peak axial strength attained by the SRM model was 198 MPa, whereas the specimen only achieved 180 MPa in the experiment.

9.2.2.5 Modelling of Specimen 14 Experiment

Modelling of Specimen 14 was carried out using the values of smooth joint micro-scale parameters from the calibrated numerical model of Specimen 1 experiment (Table 9-4). Figure 9-5 provides a visual representation of the numerical simulation results of the SRM model of Specimen 14.

Figure 9-5. Illustration of a fracture developed in Specimen 14 (a) and in the corresponding SRM specimen (b), with displacement vectors shown in (c). Smooth-joint contacts broken in tension (red disks) and in shear (green disks) are shown in (d). Axial stress-strain diagrams for the specimen (black line) and its calibrated SRM model (red line) are shown in (e).

The SRM model was able to replicate the fracture developed by Specimen 14 during the experiment. Both Specimen 14 and its numeric representation fractured along vein #1.

The SRM model attained a higher Young’s modulus value (41 GPa) than the 26 GPa value calculated from the experiment. It is believed that the reason for the discrepancy is related to the representation of veining in the model. One can see that veining was extensive in Specimen 14.
(Figure 9-5a). However, because of difficulties related to vein interpretation, the DVN contained only 6 veins. In comparison, the discrete vein networks of Specimens 1, 2, and 6 contained 23, 17, and 15 veins, respectively. Fewer number of veins in the DVN of Specimen 14 resulted in fewer smooth joint contacts in the SRM model which consequently did not lead to the reduction of macro-scale stiffness that was comparable to the level observed in the earlier numerical models.

The model was not able to reproduce the peak axial strength developed by Specimen 14. The peak axial strength attained by the SRM model was 200 MPa, whereas the specimen only achieved 142 MPa in the experiment.

9.2.2.6 Modelling of Specimen 15 Experiment

Modelling of Specimen 15 was carried out using the values of smooth joint micro-scale parameters from the calibrated numerical model of Specimen 1 experiment (Table 9-4). Figure 9-6 provides a visual representation of the numerical simulation results from the SRM model of Specimen 15.

Figure 9-6. Illustration of a fracture developed in Specimen 15 (a) and in the corresponding SRM specimen (b), with displacement vectors shown in (c). Smooth-joint contacts broken in tension (red disks) and in shear (green disks) are shown in (d). Axial stress-strain diagrams for the specimen (black line) and its calibrated SRM model (red line) are shown in (e).
The SRM model of Specimen 15 was able to replicate the fracture developed by the specimen during the experiment. Both Specimen 15 and its numeric representation fractured along vein #2.

The SRM model slightly underestimated the elastic constants of Specimen 15. The respective values of Young’s modulus and Poisson’s ratio of Specimen 15 were established to be 32 GPa and 0.15. In the model, the same parameters were 28 GPa and 0.21.

The numerical model overestimated the peak axial strength of Specimen 15. A peak strength of 207 MPa was achieved by the SRM specimen. Specimen 15 achieved a strength of 152 MPa during the experiment.

### 9.2.3 Individual Calibration of Intact Veined Specimen Models

The results of the initial SRM models suggested that the approach of using calibrated smooth joint parameters from a model of one experiment to simulate experiments of other specimens had difficulties reproducing some key experimental results. To demonstrate that numerical models are in fact capable of modelling every experiment, models were calibrated individually based on the results of each laboratory test.

Stress-strain curves from the individually-calibrated models are shown in Figure 9-7. Table 9-5 summarizes the micro-scale smooth joint parameters used to achieve calibration of SRM models for each individual triaxial experiment of Chapter 6. The calibration was carried out by adjusting two smooth-joint parameters. The scale factor for the normal stiffness was adjusted to match the elastic response of the specimen from the laboratory experiment, and smooth joint cohesion was varied to achieve the desired peak axial strength. The fracturing patterns in individually-calibrated models did not change from the ones described in Sections 9.2.2.2-9.2.2.6.
Figure 9-7. Axial stress-strain curves (in blue) for the individually-calibrated SRM models of Specimens 1, 2, 6, 14, and 15. Stress-strain curves from the experiments (in black) are provided for reference.

Table 9-5. Summary of smooth joint micro-scale parameters used to achieve calibration of SRM models for each individual triaxial experiment of Chapter 6.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Sp.1</th>
<th>Sp.2</th>
<th>Sp.6</th>
<th>Sp.14</th>
<th>Sp.15</th>
</tr>
</thead>
<tbody>
<tr>
<td>Number of veins</td>
<td>23</td>
<td>17</td>
<td>15</td>
<td>6</td>
<td>17</td>
</tr>
<tr>
<td>Normal stiffness scale factor, $\omega$</td>
<td>0.054</td>
<td>0.054</td>
<td>0.054</td>
<td>0.010</td>
<td>0.070</td>
</tr>
<tr>
<td>Ratio of normal to shear stiffness, $K_{ratio}$</td>
<td>0.7</td>
<td>0.7</td>
<td>0.7</td>
<td>0.7</td>
<td>0.7</td>
</tr>
<tr>
<td>Friction coefficient, $\mu$</td>
<td>0.5</td>
<td>0.5</td>
<td>0.5</td>
<td>0.5</td>
<td>0.5</td>
</tr>
<tr>
<td>Dilation angle, $\psi$ (degrees)</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>Radius multiplier, $\lambda$</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>Tensile strength, $\sigma_c$ (MPa)</td>
<td>10</td>
<td>10</td>
<td>10</td>
<td>10</td>
<td>10</td>
</tr>
<tr>
<td>Cohesion, $c$ (MPa)</td>
<td>63.7</td>
<td>80</td>
<td>45</td>
<td>20</td>
<td>31</td>
</tr>
<tr>
<td>Cohesion/tensile strength, $c/\sigma_c$</td>
<td>6.4</td>
<td>8.0</td>
<td>4.5</td>
<td>2.0</td>
<td>3.1</td>
</tr>
<tr>
<td>Friction angle, $\phi$ (degrees)</td>
<td>45</td>
<td>45</td>
<td>45</td>
<td>45</td>
<td>45</td>
</tr>
</tbody>
</table>

It should be noted that there are various ways to achieve a calibrated model. Different combinations of micro-scale parameters can produce a desired macro-scale response. The calibration approach adopted for this work was one of various possible methods and the set of calibration parameters established in this work represented one of many combinations.
9.2.4 Discussion of BPM Modelling Results

The numerical simulation results demonstrated that the use of SRM BPMs for modelling of intact veined rock was viable. The models were able to capture key experimental results. The modelling also highlighted few challenges related to the use of bonded particle models and the SRM method.

9.2.4.1 Specimen Fracturing

The simulation results showed that SRM BPM models could adequately capture the fracturing process observed in the laboratory tests. Similar to laboratory experiments, failure of numerical specimens was governed by existence of veins. This provides the confidence in the ability of the SRM method to account for the influence of veins in the resulting failure mechanisms.

9.2.4.2 Elastic Properties

In general, the numerical SRM models were successful at reproducing the elastic parameters observed in the triaxial experiments. Application of the micro-scale smooth joint parameters calibrated based on Specimen 1 to the models of other specimens produced reasonable agreement between the elastic moduli and Poisson’s ratios of numerical and laboratory experiments. Models based on the calibration parameters from Specimen 1 experiment showed that vein geometry alone can have an effect on specimen elasticity.

The modelling demonstrated that the number of veins, which is linked to the number of smooth joint contacts, in a DVN of a numerical specimen influenced the elastic response of the model. A numerical specimen required a minimum of 15 veins to achieve the target Young’s modulus using the calibrated value. Specimen 14, which had only 6 veins, produced a stiffer response when the calibrated value was used.

9.2.4.3 Peak Strength

The SRM models based on calibrated smooth joint parameters from Specimen 1 displayed variations of peak axial strengths. The strengths ranged between 191 MPa and 207 MPa (Figure 9-8). The resulting peak strength range was smaller than the range observed in the triaxial experiments.
Two important outcomes stem from these results. First, the models demonstrated that vein geometry, as each model used a different DVN, clearly influenced the peak axial strength of the specimen. Second, use of generic smooth joint calibration parameters developed for one specimen was not sufficient to replicate the peak strengths observed in other specimens. Strengths of individual or possibly key veins is what likely controls the failure and the peak strength of the specimen.

9.2.4.4 Axial Stress-Strain Response

There are two notable differences in the pre-peak axial stress-strain response between a specimen and its calibrated SRM model. At low axial strain, the experimental curves were concave. This was a result of specimen setting between the testing platens and initial closure of cracks within the specimen. In the numerical experiments, the crack closure stage was absent because BPM is generated as a collection of well-connected (three contacts or more) spheres. Even though the bonded particle models were porous, the application of low-magnitude loads did not cause noticeable strain of the specimens.
The second difference is related to the pre-peak material softening. In the laboratory experiments, specimens of CMET did not show noticeable softening prior to reaching their peak strengths. SRM material, however, showed noticeable pre-peak softening in numerical simulations. Because similar behaviour was observed in intact models, it is not believed to be a sole result of smooth joint contacts. The pre-peak softening is believed to be an inherent characteristic of the BPMs. It may be a by-product of the high-porosity structure of bonded particle models.

9.3 Modelling of Experiments with Various Confining Pressures using Bonded Block Models

The experiments with standard triaxial loading included testing of Specimens 5, 7, 8, 12, and 19. The experimental results were presented and discussed in Chapter 7. These experiments were simulated numerically using the bonded block modelling approach, which was discussed in Chapter 8. The results of the simulations are presented in the following sections.

9.3.1 Calibration of Intact BBM Material

9.3.1.1 Methodology

The calibration methodology utilized for BBM modelling of intact material was similar to the one used for the intact BPM models (Section 9.2.1). The four steps of the calibration process included:

1. *Calibration of elastic parameters.* The aim of this step was to reproduce the target macro-scale Young’s modulus and Poisson’s ratio of the specimen. Block elastic parameters and normal and shear stiffness of contacts were varied to achieve an agreement between the model and the target values.

2. *Calibration of tensile strength.* The purpose of this step was to match the macro-scale tensile strength of the specimen to the target value. The match was controlled by the value of the contact tensile strength.

3. *Calibration of unconfined compressive strength (UCS).* The purpose of this step was to attain a match between the UCS of the specimen and the target value. This was done by making appropriate adjustments to the contacts’ cohesion and friction angle values.
4. **Calibration of crack damage stress threshold ($\sigma_{cd}$).** The purpose of this step was for the numerical model specimen to reproduce the target value of $\sigma_{cd}$. This was achieved by introducing a variation in the tensile strength of the contacts.

### 9.3.1.2 Scale Effect

The effect of the rock specimen size on its unconfined strength is a well-known phenomenon in rock mechanics. It is known as **scale effect**. Hoek and Brown (1980) proposed an empirical relationship for intact rock between a UCS value ($\sigma_{c_{50}}$) of a standard 50 mm diameter core specimen and a UCS value ($\sigma_c$) of a specimen of arbitrary diameter ($d$) between 10 and 200 mm:

$$\sigma_c = \sigma_{c_{50}} \left(\frac{d}{50}\right)^{0.18}$$

(Eq. 9-1)

The relationship suggests that the strength decreases with the increase of specimen size. Pratt et al. (1972) suggested that the scale effect was also observed in tensile strength. Experimental results do not support presence of scale effect in the elastic parameters, Young’s modulus and Poisson’s ratio (Darlington et al. 2011; Pratt et al. 1972; Yoshinaka et al. 2008).

An explanation for the scale effect is commonly attributed to larger rock specimens containing a higher number of flaws or defects (Hoek and Brown 1980). Bonded block materials, in general, do not exhibit the scale effect in numerical models. In SRM models, it is common to scale the target intact laboratory strength with block size in calibrations of intact block strength (Mas Ivars et al. 2011). This approach was followed for the BBM modelling of intact rock: the target laboratory unconfined compressive and tensile strengths were scaled based on the inter-vein block size of intact material.

Block equivalent size ($d_e$) can be calculated from block volume ($V$) based on the Weibull’s theory as proposed by Yoshinaka et al. (2008):

$$d_e = V^{1/3}$$

(Eq. 9-2)

### 9.3.1.3 Target Macro-Scale Parameters

Based on the analyses of volumes of the inter-vein blocks from the tested specimens, the mean block size was calculated to be 12 mm. The target strengths were scaled based on the
relationship of Hoek and Brown (1980) from 14 MPa (at d=50 mm) to 18.1 MPa (at d=12 mm) for the tensile strength and from 125 MPa (at d=50 mm) to 161.6 MPa (at d = 12 mm) for the unconfined strength. A summary of target macro-scale parameters used for the calibration of micro-scale intact material properties is provided in Table 9-6.

### Table 9-6. Target macro-scale parameters for calibration of intact material micro-scale properties.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Young modulus (GPa)</td>
<td>50</td>
</tr>
<tr>
<td>Poisson’s ratio</td>
<td>0.28</td>
</tr>
<tr>
<td>Tensile strength, $\sigma_{t12}$ (MPa)</td>
<td>18.1</td>
</tr>
<tr>
<td>Unconfined compressive strength, UCS$_{12}$ (MPa)</td>
<td>161.6</td>
</tr>
<tr>
<td>Crack damage stress threshold, $\sigma_{cd}$ (% of UCS)</td>
<td>88%</td>
</tr>
<tr>
<td>Hoek-Brown constant $m_i$</td>
<td>9.1</td>
</tr>
</tbody>
</table>

#### 9.3.1.4 Calibration Results

After an extensive calibration phase, a set of micro-scale calibration parameters was derived that resulted in the macro-scale model response to be in agreement with the target response parameters (see Table 9-6). Table 9-7 provides a summary of the micro-scale calibration parameters.

In the final stage of the calibration process, three numerical specimens were generated based on the derived calibration parameters (Table 9-7). All simulated specimens were based on the original block model described in Chapter 8. The assignment of block contact strengths was different between the three specimens; contact tensile strengths were assigned based on a normal distribution. Each of the three specimens was tested in direct tension, unconfined compression, and under various confining pressures. The resulting data set was used to establish how well the specimens’ measured macro-scale responses reproduced the target parameters listed in Table 9-6.

### Table 9-7. Summary of micro-scale calibration parameters for intact material.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Intact block micro-properties</strong></td>
<td></td>
</tr>
<tr>
<td>Density, $\rho$ (kg/m$^3$)</td>
<td>2,800</td>
</tr>
</tbody>
</table>
Table 9-8 provides a summary of modelling results from the three calibrated specimens (average values are used) and the target values for the key calibration parameters. Parts a) and b) of Figure 9-9 show axial stress-strain curves for direct tension and unconfined and confined compression tests. In part c) of Figure 9-9, peak strengths from the three specimens and associated failure envelope (Hoek and Brown 1997) are plotted in the maximum and the minimum principal stress space.

Table 9-8. Summary of target parameters for calibration of intact material and their values attained by calibrated models. Attained values are averages from the 3 specimens.
<table>
<thead>
<tr>
<th>Parameter</th>
<th>Target Value</th>
<th>3DEC</th>
</tr>
</thead>
<tbody>
<tr>
<td>Unconfined compressive strength, UCS (MPa)</td>
<td>161.6</td>
<td>168.7</td>
</tr>
<tr>
<td>Crack damage stress threshold, $\sigma_{cd}$ (% of UCS)</td>
<td>88%</td>
<td>86%</td>
</tr>
<tr>
<td>Hoek-Brown constant $m_i$</td>
<td>9.1</td>
<td>9.1</td>
</tr>
</tbody>
</table>

![Figure 9-9. Results of a direct tension test (a), compression tests (b), and the corresponding Hoek-Brown strength envelope (c) for a BBM models of intact material.](image)

Figure 9-9. Results of a direct tension test (a), compression tests (b), and the corresponding Hoek-Brown strength envelope (c) for a BBM models of intact material.

Figure 9-10 provides examples of fracture patterns developed in the numerical BBM specimens during experiments at different confining pressures. Blocks in the figure are coloured by fragments. Furthermore, Figure 9-10 shows axial stress and confining pressure graph for each of the tests to illustrate the post-peak response. The models illustrating the fracturing patterns in Figure 9-10 were run until the patterns developed. Consequently, the images are not meant to represent fracturing at any particular stress or strain level.
Figure 9-10. Examples of fracture patterns developed in an intact BBM specimen when tested under different levels of confining pressure (shown above each image). Vertical cross-sections through the specimen are shown. Blocks are colored by fragment. Images below show corresponding graphs of axial stress (blue line) and confining pressure (red line) vs. axial strain.

9.3.2 Modelling of Veined Specimens

Modelling of intact veined rock aimed to reproduce the results of the triaxial compression experiments described in Chapter 7. Numerical models of veined specimens were built based on discrete vein networks presented in Chapter 4. The microscopic scale properties for the veins were selected to match the experimental results for each specimen.

9.3.2.1 Methodology

For BBM simulations, the methodology to model the triaxial experiments of intact veined rock was in part based on the methodology developed for modelling of intact veined rock using bonded particle models. The approach consisted of the following steps:
1. **Generation of veined specimen.** A block model, containing vein geometry of a modelled specimen, was created for each specimen. This was described in Chapter 8.

2. **Calibration of vein tensile strength.** The purpose of this step was to calibrate the tensile strength of veins in numerical models to match the tensile strength from experimental data.

3. **Calibration of elastic parameters.** The aim of this step was to reproduce the experimental elastic parameters.

4. **Calibration of confined compressive strength.** The purpose of this step was to match peak strengths in numerical simulations to the ones observed in the laboratory experiments.

**9.3.2.2 Calibration of Vein Tensile Strength**

Jacobsson et al. (2011) reported on direct tension tests of veins of the ore body rock from El Teniente. Six experiments were conducted involving veins that were predominantly composed of anhydrite and chalcopyrite. The tensile strengths ranged between 0.4 and 3.5 MPa, with an average of 1.65 MPa.

Calibration of vein tensile strength was carried out using the model of intact material into which a single horizontal vein, positioned in the centre of the specimen, was introduced. The model was subjected to a direct tension test. The mean tensile vein strength of 1.65 MPa was used as the macro-scale vein strength. The calibration results showed that the target macroscopic strength can be replicated using the micro-scale contact tensile strength of 1.05 MPa.

**9.3.2.3 Calibration of Specimen Elasticity**

The Young’s modulus value of intact rock was taken to be 50 GPa. Triaxial experiments on the specimens of intact veined rock demonstrated a range of Young’s moduli between 30 and 34 GPa. In order to achieve the necessary reduction in modulus from the value of intact rock (i.e. without veins) to the one obtained experimentally, the normal stiffness of contacts representing veins in a model of veined specimen was reduced. The amount of the reduction was specific to each specimen, and a calibration approach was used to determine appropriate vein normal stiffness values.

The ratio of normal to shear stiffness was selected to be equal to 1. A series of models were run to establish the value of vein normal stiffness such that the response of the model under the confining pressure of the experiment matched the Young’s modulus values from the experiment.
In the numerical simulation, all contacts representing veins were assigned the same stiffness value. Table 9-9 summarizes the results of the vein stiffness calibration.

**Table 9-9. Summary of target and resulted values for calibration of specimen stiffness’s and micro-scale vein stiffness values used for a numerical model of each specimen.**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Sp.19</th>
<th>Sp.12</th>
<th>Sp.7</th>
<th>Sp.5</th>
<th>Sp.8</th>
</tr>
</thead>
<tbody>
<tr>
<td>Confining pressure (MPa)</td>
<td>2</td>
<td>5</td>
<td>30</td>
<td>45</td>
<td>60</td>
</tr>
<tr>
<td>Number of veins</td>
<td>11</td>
<td>13</td>
<td>21</td>
<td>13</td>
<td>16</td>
</tr>
<tr>
<td>Target / attained Young’s modulus (GPa)</td>
<td>33.7 / 33.8</td>
<td>33.3 / 33.3</td>
<td>30.0 / 29.7</td>
<td>33.2 / 33.0</td>
<td>34.0 / 34.0</td>
</tr>
<tr>
<td>Target / attained Poisson’s ratio</td>
<td>0.24 / 0.22</td>
<td>0.19 / 0.19</td>
<td>0.22 / 0.21</td>
<td>0.11 / 0.19</td>
<td>0.16 / 0.19</td>
</tr>
<tr>
<td>Vein normal stiffness, $k_n$ (MPa/m)</td>
<td>$3.5 \times 10^6$</td>
<td>$5.0 \times 10^6$</td>
<td>$2.47 \times 10^6$</td>
<td>$5.0 \times 10^6$</td>
<td>$5.2 \times 10^6$</td>
</tr>
<tr>
<td>Vein normal / shear stiffness ratio, $K_{rat}$</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
</tr>
</tbody>
</table>

The results demonstrated that specimen stiffness required individual calibration and that values calibrated based on one specimen could not be used for other specimens. The number and the arrangement of veins in the specimen influenced the stiffness of a numerical specimen.

**9.3.2.4 Calibration of Specimen Peak Strengths**

The final step of modelling included running confined models for each of the triaxial experiments, calibrating vein strength properties to match the peak strengths recorded in the laboratory test. The approach was similar to the one used in calibrating the intact rock models. The value of the micro-scale tensile strength for the contacts representing veins was fixed at the calibrated value of 1.05 MPa. The friction angle value was selected to be 45°. The ratio of cohesion to tensile strength was varied to match the peak strength in each experiment. Because pre-peak strain is small, residual friction and dilation angle did not affect the strength, so these parameters were set to 0 MPa and 0°, respectively.

The calibration of peak strength was achieved for every model. Table 9-10 summarizes the key results from calibrated models, comparing them to the ones from the experiments. Table 9-11 summarises microscopic scale properties for vein contacts used in the calibrated numerical models. The modelling results are presented and discussed individually for each numerical simulation.
Table 9-10. Comparison between key experimental and calibrated model results for numerical experiments. Paired values represent experimental and modelling results.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Sp.19</th>
<th>Sp.12</th>
<th>Sp.7</th>
<th>Sp.5</th>
<th>Sp.8</th>
</tr>
</thead>
<tbody>
<tr>
<td>Confining pressure (MPa)</td>
<td>2</td>
<td>5</td>
<td>30</td>
<td>45</td>
<td>60</td>
</tr>
<tr>
<td>Exp. / model peak strength, $\sigma_f$ (MPa)</td>
<td>162.5 / 167.4</td>
<td>167.5 / 169.5</td>
<td>229.6 / 232.0</td>
<td>320.1 / 323.8</td>
<td>377.1 / 363.8</td>
</tr>
<tr>
<td>Exp. / model crack initiation stress, $\sigma_{ci}$ (MPa)</td>
<td>125 / 135</td>
<td>132 / 142</td>
<td>171 / 157</td>
<td>188 / 182</td>
<td>219 / 211</td>
</tr>
<tr>
<td>Exp. / model $C'/C = \frac{(\sigma_{ci} - p_c)}{(\sigma_f - p_c)}$</td>
<td>0.77 / 0.80</td>
<td>0.78 / 0.83</td>
<td>0.71 / 0.63</td>
<td>0.52 / 0.49</td>
<td>0.50 / 0.50</td>
</tr>
<tr>
<td>Exp. / model crack damage stress, $\sigma_{cd}$ (MPa)</td>
<td>158 / 165</td>
<td>163 / 167</td>
<td>228 / 231</td>
<td>285 / 315</td>
<td>365 / 354</td>
</tr>
<tr>
<td>Exp. / model $D/C = \frac{(\sigma_{cd} - p_c)}{(\sigma_f - p_c)}$</td>
<td>0.97 / 0.98</td>
<td>0.97 / 0.98</td>
<td>0.99 / 0.99</td>
<td>0.87 / 0.97</td>
<td>0.96 / 0.97</td>
</tr>
</tbody>
</table>

Table 9-11. Summary of vein contact micro-scale properties for calibrated numerical models.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Sp.19</th>
<th>Sp.12</th>
<th>Sp.7</th>
<th>Sp.5</th>
<th>Sp.8</th>
</tr>
</thead>
<tbody>
<tr>
<td>Confining pressure (MPa)</td>
<td>2</td>
<td>5</td>
<td>30</td>
<td>45</td>
<td>60</td>
</tr>
<tr>
<td>Normal stiffness (MPa/m)</td>
<td>$3.5 \times 10^6$</td>
<td>$5.0 \times 10^6$</td>
<td>$2.47 \times 10^6$</td>
<td>$5.0 \times 10^6$</td>
<td>$5.2 \times 10^6$</td>
</tr>
<tr>
<td>Normal to shear stiffness ratio</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>Tensile strength, $\sigma_{tmax}$ (MPa)</td>
<td>1.05</td>
<td>1.05</td>
<td>1.05</td>
<td>1.05</td>
<td>1.05</td>
</tr>
<tr>
<td>Residual tensile strength, $\sigma_{tr}$ (MPa)</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>Cohesion / tensile strength ratio, $c_{ratio}$</td>
<td>100</td>
<td>40</td>
<td>170</td>
<td>300</td>
<td>300</td>
</tr>
<tr>
<td>Friction angle, $\phi$ (degrees)</td>
<td>45</td>
<td>45</td>
<td>45</td>
<td>45</td>
<td>45</td>
</tr>
<tr>
<td>Residual friction angle, $\phi_{tr}$ (degrees)</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>Dilation angle, $\psi$ (degrees)</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>Displacement for zero dilation, $U_{lim}^\delta$ (m)</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
<td>0</td>
</tr>
</tbody>
</table>
9.3.2.5 Modelling of Specimen 19 Experiment

Figure 9-11 illustrates the results of the simulation of the experiment on Specimen 19 which was tested at the confining pressure of 2 MPa. The model slightly overestimated the peak strength (Figure 9-11e). Good correspondence was achieved for the crack initiation and crack damage stress thresholds (Table 9-10).

![Figure 9-11. Illustration of a fracture developed in Specimen 19 (a) and in the corresponding SRM specimen (b). Cross-sections illustrating contact shear and normal displacements are shown in (c) and (d). Axial stress-strain diagrams for the specimen (black line) and its calibrated SRM model (blue line) are shown in (e).](image)

The fracturing of the specimen during the laboratory experiment was governed by the presence of the weak chalcopyrite vein (#5) transcending the specimen at a shallow angle (Figure 9-11a). Other fractures within the specimen developed subsequently to the initial shear failure along vein #5. In the numerical experiment, the failure surface followed partially the contacts representing veins, including the contacts representing vein #5. The failure, however, also developed through the block contacts representing intact material (Figure 9-11b,c). The angle of
the fracture surface is consistent with the results observed in the models of intact rock. Figure 9-11d shows that the ends of the numerical specimen were under tensile forces.

### 9.3.2.6 Modelling of Specimen 12 Experiment

Figure 9-12 illustrates the results of the simulation of the experiment on Specimen 12 which was tested at the confining pressure of 5 MPa. The SRM model overestimated the peak strength by 2 MPa. Good agreement was achieved for the crack initiation and crack damage stress thresholds between the laboratory and numerical experiments (Table 9-10).

![Figure 9-12. Illustration of a fracture developed in Specimen 12 (a) and in the corresponding SRM specimen (b). Cross-sections illustrating contact shear and normal displacements are shown in (c) and (d). Axial stress-strain diagrams for the specimen (black line) and its calibrated SRM model (blue line) are shown in (e).](image)

The numerical model was able to replicate the fracturing characteristics of the laboratory experiment, reproducing multiple fractures. Figure 9-12a,b show that the specimen failure in the model occurred along the same veins (#2 and #3 veins) as in the experiment. Figure 9-12c shows that the failure occurred in shear, and Figure 9-12d suggests that some areas with contact forces in
tension developed within the specimen, mostly in the vicinity of the veins experiencing shear displacements.

9.3.2.7 Modelling of Specimen 7 Experiment

Figure 9-13 illustrates the results of the simulation of the experiment on Specimen 7 which was tested at the confining pressure of 30 MPa. The SRM model slightly overestimated the peak strength (Table 9-10). The numerical model’s axial stress / axial strain curve showed gradual failure (vs abrupt in the experiment) failure. The model underestimated the experimental crack initiation stress results by approximately 8% (Table 9-10). The crack damage stress thresholds in the experiment and the model were in agreement.

Figure 9-13. Illustration of a fracture developed in Specimen 7 (a) and in the corresponding SRM specimen (b). Cross-sections illustrating contact shear and normal displacements are shown in (c) and (d). Axial stress-strain diagrams for the specimen (black line) and its calibrated SRM model (blue line) are shown in (e).

In the laboratory experiment, Specimen 7 fractured along vein #1 (Figure 9-13a). Exact fracturing mode of the numerical model was indeterminate. The model was not able to develop enough separation between blocks as it could not run past approximately 1% axial strain due to its
large size (over 9 GB). However, analyses of blocks detached from the specimen’s surface (Figure 9-13b) and of contact shear displacements (Figure 9-13c) suggest that the specimen was in the process of developing shear fractures, which are marked in Figure 9-13b. One of the potential fractures was positioned and aligned similar to the fracture that developed in the laboratory specimen.

Figure 9-13d suggests that the block contacts located at the ends of the numerical specimen were in tension. The values of contact tensile stresses did not exceed the tensile strength of intact rock contacts. Therefore, the existence of these tensile regions did not suggest fracturing of the specimen.

9.3.2.8 Modelling of Specimen 5 Experiment

Figure 9-14 illustrates the results of the simulation of the experiment on Specimen 5 which was tested at the confining pressure of 45 MPa. The numerical model overestimated the peak strength by approximately 3 MPa. Good correspondence was achieved for the crack initiation stress threshold (Table 9-10). The crack damage stress threshold was slightly higher in the numerical model.

In the laboratory experiment, Specimen 5 along a single vein (Figure 9-14a). Due to the size of the model, the numerical simulation was not able to continue long after reaching the peak strength. Consequently, the separation of blocks that would clearly indicate specimen fracturing pattern was not achieved. However, the location of the fractures developed on the surface of the numeric specimen are consistent with the location and orientation of the vein along which laboratory specimen fractured (Figure 9-14b).

Figure 9-14c did not indicate that shearing occurred along block contacts. This was likely due to the early stages of the specimen failure. Presence of zones where contact stresses were tensile, however, was evident (Figure 9-14d).
Figure 9-14. Illustration of a fracture developed in Specimen 5 (a) and in the corresponding SRM specimen (b). Cross-sections illustrating contact shear and normal displacements are shown in (c) and (d). Axial stress-strain diagrams for the specimen (black line) and its calibrated SRM model (blue line) are shown in (e).

9.3.2.9 Modelling of Specimen 8 Experiment

Figure 9-15 illustrates the results of the simulation of the experiment on Specimen 5 which was tested at the confining pressure of 60 MPa. The numerical model underestimated the peak strength by approximately 13 MPa. The discrepancy between the peak strengths attained by Specimen 8 in the laboratory experiment and in the numerical model was less than 4%. Other parameters were in agreement between the experiment and the numerical simulations (Table 9-10).
Figure 9-15. Illustration of a fracture developed in Specimen 8 (a) and in the corresponding SRM specimen (b). Cross-sections illustrating contact shear and normal displacements are shown in (c) and (d). Axial stress-strain diagrams for the specimen (black line) and its calibrated SRM model (blue line) are shown in (e).

In the laboratory experiment, Specimen 8 fractured along several veins (#1, #3, #8, and #9), with the main fracture transcending the specimen at an angle of 63° (vein #1) to the specimen’s diameter (Figure 9-15a). In the numerical model, the fracture followed vein #3.

The contact shear displacement pattern is illustrated in Figure 9-15c. The fracturing of the numerical specimen occurred in shear. The plot of contact tensile displacements (Figure 9-15d) shows that the ends of the numerical specimen were experiencing tensile forces.

9.3.3 Discussion of BBM Modelling Results

The results of the numerical modelling demonstrated that application of the bonded-block approach was successful in modelling of intact veined rock. Models calibrated based on veined network geometries, vein tensile strength, and peak specimen strengths allowed reproduction of key behaviours observed in the laboratory experiments.
9.3.3.1 Specimen Fracturing

In general, the BBM models were able to reproduce fracturing observed in the experiments. In four models, fractures developed along contacts that represented specimen veins, similar to the experiments. The ability of BBM models to adequately reproduce specimen fracturing reinforces the idea of using SRM for modelling of intact veined rock.

9.3.3.2 Elastic Properties

The experience with calibrating specimen elasticity in the BBM models was similar to the one observed in the BPM models. The need of the veined specimens to reproduce a much lower Young’s modulus in comparison to the value of intact rock required softening of the contacts representing veins. The degree of required softening was not universal for all specimens. It depended on the number of vein contacts and therefore was somewhat related to the number of veins within the specimen. The modelling results showed that it was also related to vein orientation, i.e. it was dependent on a DVN.

9.3.3.3 Vein Tensile Strength

The micro-scale vein tensile strength was set relatively low at 1.05 MPa for all vein contacts in the simulations. In comparison, the tensile strength of contacts within the intact portion of the specimens was set much higher, at an average value of 40.4 MPa. The models demonstrated that vein contacts did not fail in tension but in shear. Shear strength of vein contacts was the parameter controlling the strength of the specimens. The implication of this is that in SRM numerical models on intact veined rock selection of tensile strength for vein contacts is not critical and any reasonable value will likely be sufficient.

9.3.3.4 Axial Stress-Strain Response

All axial stress-strain curves of the BBM models demonstrated steeper response at low axial strain. In the numerical experiment, the initial steep portion of the curve coincided with the increase in the confining pressure. Once the target confining pressure was reached, the curve became less steep, corresponding to the target stiffness. Similar effects were observed in experimental data. Experiment on Specimen 8 with the confining pressure of 60 MPa is a good example (Figure 9-15d). The initial steepening of the stress-strain curve in the experiments was somewhat masked by the crack closure stage. However, the stress-strain curve is noticeably steeper before the confining pressure reached its maximum, flattening in the constant confinement stage.
of the experiment. The reason why this effect is so pronounced in the numerical models is because BBM models have zero initial porosity and do not show the initial crack closure stage.

9.4 Conclusions

Numerical simulations of the triaxial tests on intact veined rock aimed to reproduce key behaviours observed in the tests. The modelling relied on the use of the SRM approach. The first set of experiments with the “caving” stress path (Chapter 6) was modelled using the SRM+BPM combination with the PFC3D software. The experiments with various confining pressures (Chapter 7) were modeled using the SRM+BBM approach with the 3DEC numerical code. The change from the BPM to the BBM implementation of the SRM framework was governed by a number of limitations associated with the use of bonded particle models in representing behaviour of intact rock, which were discussed in Chapter 8. The bonded block models allowed to overcome the limitations, ultimately producing better results.

To date, the application of the SRM technique in modelling of veined rock has been very limited in the industry, and SRM models with clumped particles have not been used before. SRM numerical models developed based on BBM are entirely unique to this research. Both sets of models allowed to evaluate application of the SRM method to intact veined rock, compare the BPM and BBM techniques, and to gain more information that defined the behaviour of intact veined rock in compression. Conclusions of this research that stemmed from the numerical simulations are summarized below.

Modelling Methodology

The developed methodology specifically addressed the use of the SRM for modelling of intact veined rock. The results demonstrated that the methodology was successful at modelling of triaxial experiments under compression. Both BPM and BBM models were able to reproduce key behaviours of core-sized intact veined rock specimens that were observed in the laboratory experiments.

Accuracy of the vein networks constructed using the methodology developed for this research (Chapter 4) was sufficient to capture the behavior of the specimens during the simulations. The modelling results demonstrated that the veins could be represented as 3D surfaces and modelled adequately using smooth joint and block contacts in PFC3D and 3DEC numerical codes.
Clumped BPM vs. BBM Models

The results of the numerical simulations demonstrated that clumped BPM models were superior to BPM models based on spherical particles. But the improvements in the behavior of intact rock given by the use of clumps were not enough to reproduce set forth intact rock properties.

The modelling results demonstrated that BBM models were more successful than clump-based BPM models in reproduction of intact material behaviour based on target parameters. Calibration of BPM models based on both the tensile and unconfined compressive strengths was not successful. Parallel-bonded particle material was not able to reproduce both characteristics in a single model. Bonded block models were not limited in this respect and were able to reproduce the target tensile and unconfined compressive strengths, as well as the failure envelope.

Specimen Stiffness

Calibration of numerical models demonstrated that controlling specimen stiffness by uniformly changing the stiffness of all contacts that represented veins was a successful approach. Use of identical stiffness parameters highlighted the fact that geometric arrangement of veins within a specimen, which was expressed by a vein network, was able to influence the specimen’s elastic parameters.

Application of a uniform stiffness value to all vein contacts in the specimen worked well in some models but was less successful in others. The modelling highlighted the need in applying vein contact stiffness based on characteristics of individual veins. Development of such approach was outside the scope of the current work.

Role of Veins in the Specimen Failure

Numerical simulations demonstrated that failures of numerical specimens were governed by veins, similar to the failures observed the laboratory experiments. This comprised one of the main outcomes of the simulations. It demonstrated that the SRM method can be used to model veined rock, creating confidence for similar applications of SRM in the future.

Numerical models demonstrated that a vein network was able to influence the specimen’s peak strength. However, a DVN alone did not determine the strength of the specimen. Use of
generic vein calibration parameters developed based on one specimen was not sufficient to replicate the behaviour and strength observed in another specimen. Strengths of individual or possibly key veins was what likely controlled the failure and the peak strength of the specimen.

The simulation results demonstrated that the strength of a specimen was a function of vein characteristics. Parameters of individual veins, such as shear strength, stiffness, and location and orientation, all have the ability to influence specimen strength. Vein shear strength determines the peak shear stress a vein is able to sustain prior to fracturing. Vein stiffness affects deformation characteristics and shear stresses generated on a vein. Vein position and orientation affects the amount of shear stress it is subjected to during a simulation. The results of the modelling process indicated that all of these parameters need to be taken into consideration for each vein in the simulations.

**Tensile Strength of Veins**

The simulations results showed that the tensile strength of the contacts representing veins played a minor role in determining the specimen strength in compression. The implication of this is that in SRM numerical models of experiments on intact veined rock in compression, selection of tensile strength for vein contacts is not critical and a reasonable value is likely to be sufficient.

Both experimental results and numerical simulations contributed to the development of understanding of how intact veined rock behaves in compression. The last chapter (Chapter 10) concludes the thesis, providing a summary of the findings, discussing potential practical applications of this work, its contributions, limitations, and recommendations for future work.
Chapter 10
Conclusions

10.1 Chapter Outline

This chapter provides the conclusions of this thesis on the characterization, behaviour, and modelling of intact veined rock. It reiterates the motivation and the justification for this work, the objectives, and the undertaken methodology. The chapter presents the main contributions of this thesis while at the same time acknowledging its limitations. This chapter concludes with recommendations for further work and development.

10.2 Motivation and Originality of the Thesis

The motivation for this thesis lies in our limited understanding of the influence of mineral veins on the behaviour of rock. Studying the effects of veining on the rock behaviour and its strength is key for mass mining operations in massive veined rock masses. Veins influence the initiation and propagation of caving, as well as secondary fragmentation of caved material.

The primary objective of this work was to develop an understanding and characterize the behaviour of intact veined rock under compressive loads. The secondary objective was to develop an approach for numerical modelling of intact veined rock.

In order to accomplish the objectives of this thesis the following tasks were undertaken:

- Site visit and selection of suitable samples for laboratory experiments;
- Characterization of intact rock specimens, construction of discrete vein networks (DVN), and petrographic analyses of vein mineralogy;
- Laboratory triaxial testing with acoustic emission monitoring; and,
- Development and execution of numerical modelling procedures.

10.3 Contributions

10.3.1 Development of DVN Methodology

One of the key contributions of this work includes development of the methodology for constructions of discrete vein networks for intact veined rock specimens (Figure 10-1). Using specimen photographs, veins are initially traced in 2D which results in construction of a flat
representation of vein geometry. Vein locations and termination characteristics are honored by the process. The 2D vein traces are then wrapped around a cylinder. The process is completed with construction of mesh segments that are fitted through the vein traces. Examples of discrete vein networks produced using the developed methodology are illustrated in Figure 10-2.

Figure 10-1. Methodology developed for construction of discrete vein networks for intact veined rock specimens.

Figure 10-2. Examples of the resulting discrete vein networks.
10.3.2 Vein Characterization

Comprehensive characterization of veins included collection and analysis of data on vein geometry, thickness and orientation, and vein mineralogy. Veins included in specimen DVNs were catalogued. Vein thickness and orientation data were established based on measurements from DVNs. The petrographic assessment of vein mineralogy was carried out based on thin sections (Figure 10-3).

![Image of thin section analysis]

Figure 10-3. Illustration of the thin section analysis.

10.3.3 Triaxial Experiments

Two sets of triaxial experiments were designed and performed as part of this work. The purpose of the first set was to generate high quality data for development of numerical models. Five experiments followed an identical stress path that mimicked the loading that a rock mass experiences during passing of a caving front. Use of an identical loading conditions allowed to evaluate how vein network and mineralogy influenced the strength. The stress path was similar to the one observed during the propagation of a cave front at the Esmeralda sector of El Teniente.

The purpose of the second set of experiments was to evaluate how different levels of confinement affected the behaviour of intact veined rock. Standard triaxial loading conditions were used. Specimens were tested under confining pressures of 2, 5, 30, 45, and 60 MPa.
The experimental investigations demonstrated that the generalized behavior of intact rock under load as presented by Martin (1993) was not capturing the distinct behavior of intact veined rock. To these purposes a new diagram (Figure 10-4) was developed specific to intact veined rock.

**Figure 10-4. Generalized behaviour of intact veined rock in compression.**

**10.3.4 Modelling of Intact Veined Rock**

Two techniques for numerical simulations of intact veined rock were developed as part of this work. The techniques were based on the synthetic rock mass (SRM) methodology which had been originally developed for explicit modelling of jointed rock masses. SRM was used for the first time in this work to model intact veined rock.
SRM models based on clumped bonded particle models (BPM) were used to simulate the first set of laboratory experiments on intact veined rock (Figure 10-5). Use of BPM SRM models with clumped particles has not been attempted prior to this work. It had been shown before that particle clumping in 2D models resolved some of the limitations associated with BMP modelling of intact rock. The 3D numerical models of this work demonstrated that use of particle clumping, while showing improvements compared to unclamped models, was not successful for attaining of target intact rock characteristics.

![Figure 10-5. Illustration of the results of bonded particle SRM models in PFC3D.](image)

SRM models based on bonded block models (BBM) were used for simulations of the second set of experiments on intact veined rock (Figure 10-6). Use of BBM with SRM has not been attempted prior to this work. It was demonstrated that BBM models were capable of reproducing key characteristics of intact rock, and therefore BBM models were superior to BPM models in modelling of intact rock.
10.4 Summary of Results

10.4.1 Methodology

Triaxial compression experiments were conducted on 10 intact veined rock specimens from the Mafic Intrusive Complex (CMET) of the El Teniente mine (Chile). Cylindrical 50 mm diameter 125 mm long core specimens were prepared and tested at the University of Toronto in accordance with the recommendations for testing of intact rock (ISRM 1978). Each specimen was a subject of extensive characterization, including characterization of vein geometry and vein mineralogical composition. Numerical models, developed to simulate the experiments, were based on the synthetic rock mass (SRM) approach, employing both the PFC3D and 3DEC distinct element codes.

10.4.2 Vein Characteristics

Geometrical characterization of veins included their cataloguing and construction of discrete vein networks based on a comprehensive methodology that was developed as part of the study. The tested specimens of intact veined rock contained between 6 and 23 veins.
For each mapped vein, its average thickness and angle with respect to the diametric axis of the specimen were measured. The vein thickness analysis demonstrated that values followed two power-law distributions. The distribution of veins having thickness less than 1.5 mm was characterized by a scaling factor of 0.7. The distribution of veins having thickness greater than 1.5 mm was characterized by a scaling factor of 2.1.

Analyses of vein angles demonstrated that vein orientations varied in the specimens from being parallel to normal with respect to the short axis. In many specimens, the angles followed nearly uniform distributions. All specimens contained veins inclined 50-70° with respect to the short axis, being preferentially-oriented for the development of a shear-type failure in triaxial compression experiments.

Vein mineralogical analysis demonstrated that anhydrite and quartz were two most dominant infill minerals; other main minerals occurring inside veins included plagioclase, biotite, chlorite, chalcopyrite, k-feldspar, and muscovite. Petrographic analysis did not indicate any correlations between vein mineralogy, thickness or inclination.

10.4.3 Experimental Results

The results of the laboratory compression experiments suggested that the behaviour of the tested specimens of intact veined rock was influenced by specimen orientation in-situ with respect to the direction of the regional in-situ maximum principal stress (σ₁) and by the presence and characteristics of mineral veins.

In every tested specimen, P-wave velocities measured along the direction of σ₁ were higher than the velocities measured normal to the direction of σ₁. It was concluded that the specimens likely contained pre-existing microscopic fractures oriented subparallel to the direction of σ₁. One of the effects of such orientation was observed in an apparent reduction of peak strength in the specimens that were cored subparallel to the σ₁ direction.

Triaxial compression experiments demonstrated that fracturing of the intact veined rock specimens was controlled by veins, with veins being the weakest link in the system. Therefore, the strength of the veins was what determined the overall strength of the specimens.
The veins influenced the strength of the specimens in two ways: the orientation with respect to the axial load and content of hard minerals in their infill. Orientation of veins with respect to the axial load played a key role in determining which veins would be susceptible to fracture propagation. In most of the experiments, specimens ruptured on a vein or a combination of veins that were inclined at an angle between 10° and 40° with respect to the axial load (50°-80° relative to the diametric axes). Even if potentially ‘weaker’ (see below) veins were present in the specimen, if their orientation was not in the ideal range for propagation of a shear-type fracture, they did not participate in the fracture of the specimen. Based on these observations it is concluded that vein orientations were more critical to specimen failure than strengths of individual veins in the specimen.

The experiments demonstrated that the mineral composition of veins also affected the peak strengths of the specimens. Following the approach adopted by Brzovic (2010), minerals were designated as soft (Mohs hardness ≤ 4) or hard (Mohs hardness > 4). In the triaxial experiments, specimens that ruptured on veins with infill dominated by hard minerals attained relatively higher strengths than the specimens in which infill in the fractured veins was dominated by soft minerals.

A linear relationship was observed between the experimental crack initiation stress threshold (σ\text{ci}) values, which mark the onset of dilatancy, and the confining pressure (p\text{c}). At confining pressure above 30 MPa, the ratios of stress difference at the σ\text{ci} level to the stress difference at peak strength (C'/C) were equal to 0.5 – the expected level for intact specimens of crystalline rock (Brace et al. 1966; Scholz 1968). In experiments with the confining pressure of 30 MPa or less, C'/C values ranged between 0.7 and 0.9, much higher than 0.5. Increased C'/C ratios for the experiments with p\text{c}≤30 MPa are attributed to lowered peak strengths of the specimens which result from the presence of weakening features – veins.

The intact veined rock specimens also demonstrated a short stage of unstable fracturing that was atypical of intact rock. In experiments on intact rock, the ratios of stress difference at the σ\text{cd} level to the stress difference at peak strength (D/C) are normally observed to be near 0.8. In the experiments on intact veined rock, the D/C values were between 0.87 and 0.99. Rupture of the specimens followed closely the crack damage stress threshold.
The tested specimens of intact veined rock did not display significant acoustic emission (AE) activity prior to reaching the peak strengths. Unlike in compression experiments on intact rock, in which the onset of AE activity tends to correlate with the onset of specimen dilatancy (i.e. $\sigma_i$), for specimens of intact veined rock, the onset of AE correlated with the damage initiation stress ($\sigma_{cd}$). This suggests that in experiments on intact veined rock, the onset of AE activity should not be used for detection of specimen dilatancy as it can lead to misleading interpretations.

10.4.4 Strength Envelope

Based on the results of triaxial tests, the value of the Hoek-Brown $m_i$ constant was calculated to be 9.6, which is consistent with other published values for the El Teniente’s CMET rock (e.g. Brzovic 2010). The $m_i$ value is relatively low for a crystalline igneous rock (andesite). This is the direct result of veins being present in the specimens, acting as weak mechanical features.

10.4.5 Numerical Simulations

The application of the SRM approach to modelling of triaxial compression experiments on intact veined rock was proven to be successful. Both bonded particle models (BPM) and bonded block models (BBM) were able to reproduce key behaviours observed in the experiments. Numerical simulations demonstrated that the failure of numerical specimens was governed by veins, similar to laboratory experiments.

BBM models based on tetrahedral blocks were more successful than clump-based BPM models in reproduction of intact material behaviour. Calibration of BPM models based on both tensile and unconfined compressive strengths was not successful. Parallel-bonded PFC3D specimens were not able to reproduce both characteristics in a single model. Bonded block models were able to reproduce the target tensile and unconfined compressive strengths, as well as the target failure envelope.

Numerical models demonstrated that the geometry of a vein network, with all other vein parameters being equal, was capable of influencing the specimen’s peak strength. With other parameters playing a role in specimen strength, this finding was only made possible by numerical simulations.
The simulation results demonstrated that the tensile strength of the contacts representing veins played a minor role in determining the specimen strength. The implication of this is that in SRM simulations of intact veined rock in compression, selection of tensile strength for vein contacts is not critical and selecting of reasonable value is sufficient.

The simulation results demonstrated that the strength of a specimen was a function of vein characteristics. Parameters of individual veins, such as shear strength, stiffness, and location and orientation, all have ability to influence specimen strength. Vein shear strength determined the amount of shear stress that the vein was able to sustain prior to fracturing. Vein stiffness affected deformation characteristics and shear stresses generated on vein contact. Vein position and orientation affected the amount of shear stress to which the vein was subjected during a simulation. The results of the modelling process indicated that all of these parameters need to be taken into consideration for each vein in the simulations.

10.5 Potential Practical Implications

Several researchers (e.g. Diederichs et al. 2004; Martin and Christiansson 2009) drew parallels between the results of laboratory tests on brittle intact rock and the behaviour of massive brittle rock masses. This section of the thesis discusses how the particularities of the observed intact veined rock behaviour may influence our current interpretations of the behaviour of massive veined rock masses.

This research has been focused on the behaviour of veined rock on the scale of an intact laboratory specimen. It is feasible that similar behaviour may be observed at a larger scale, such as of a drift, a stope or a mine. Because characteristics of intact rock shape the character of the rock mass, provided that the behaviour translates from laboratory to field scale, intact veined rock has the most potential to determine the behaviour of massive veined rock masses where jointing is sparse or non-existent, such as at the El Teniente mine.

10.5.1 Estimation of Strength of Massive Veined Rock

Accurate determination of rock mass strength remains one of the most challenging tasks in geomechanics. In investigations on the strength of massive competent rock masses under high stress, Martin and Christiansson (2009), Diederichs et al. (2004), and Martin (1993) concluded that the lower boundary of the spalling rock mass strength range can be reliably estimated to be
equal to the $\sigma_{ci}$ obtained from unconfined compression experiments on intact rock. Because true crack initiation stress thresholds are rarely studied outside of research applications, it is common in engineering practice to estimate $\sigma_{ci}$ to be $0.4 \pm 0.1$ of the UCS. This is based on our general understanding of intact rock failure process (Chapter 5).

For massive veined rock masses, such as CMET at El Teniente, estimation of the spalling rock mass strength based on UCS alone will likely lead to an underestimation of strength. For example, if an average 125 MPa value of UCS is used, the approach based on the fraction of UCS will result in estimation of the spalling limit for CMET to be $50 \pm 13$ MPa. If the crack initiation stress threshold approach is used, however, the spalling rock mass strength would be estimated to be approximately 100 MPa, somewhat 2 times higher than the initial estimate.

Potential underestimation of rock mass strength in massive rock may have widespread ramifications for a mining operation. Some negative effects include over-support of underground excavations, under-sizing of production stopes, and overestimating of potential dilution. Over-support of underground workings increases costs and time associated with support installation. This can negatively affect economics and viability of a mining project. In sizing of mining stopes, underestimation of strength will ultimately result in the use of smaller stopes, also affecting economics of the project. The estimation of increased dilution may negatively affect assessment of the project economics.

Underestimation of the spalling strength of rock mass may potentially lead to overly optimistic assessments of the rock mass caveability and fragmentation. This may result in underestimation of the size of an undercut required for initiation of orebody caving, caving propagation, and resulting fragment size reporting to the drawpoints. The economic impact of any of these can be very significant and can influence profitability of an operation.

10.5.2 Rock Mass Fracturing, Cavability, and Fragmentation

Provided that the presence of veins in-situ has similar bearing on the rock mass as it does on an intact specimen, mineralogical characteristics and orientation of veins must be considered in the assessment of rock mass fracturing, cavability, and fragmentation. Brzovic (2010) demonstrated that mineral veins were key to the disassembly of the massive veined primary ore at the El Teniente mine during caving and subsequent draw. Using observations, he concluded that
natural fragmentation of primary ore was influenced by veins that contained less than approximately 30% of hard minerals (Moh’s hardness > 4). The experiments on intact veined rock demonstrated similar results.

The experiments also demonstrated that vein orientations with respect to the direction of the major principal stress determined how specimen fractured. This suggests that in rock masses with predominant vein orientation, caving can be promoted by undercutting the orebody such that the stresses induced in the caving zone are aligned 20-40° with respect to the orientation of the dominant vein set.

In situations where orientation of the veins is random and cannot be easily exploited, the undercutting strategy may take advantage of the micro cracks that develop in-situ sub-parallel to the direction of the regional major principal stress (Plumb et al. 1984). By undercutting the orebody such that the stresses induced in the caving zone are aligned with the direction of $\sigma_1$, it may be possible to employ stress caving in situations where in-situ stresses are moderate or rock mass is very competent (Brown 2003).

10.6 Research Limitations

During the course of the research, several limitations related to the conducted work have been recognized. These were associated with the experimental work and numerical simulations.

The recognised limitations associated with the experimental work are as follows:

- The main limitation of the experimental work is the limited number (10) of tested specimens which were collected in the same area of the mine. However, with a limited number of specimens, it became possible to focus the research on collecting detailed quality experimental data rather than rely on a large number of lower-quality experiments. Having the specimens from the same part of the mine also ensured that they were similar in structure and mineralogy, allowing for greater control in the experiments.
- The experiments have focused on one rock type – CMET of the El Teniente mine. Consequently, the presented and discussed results are specific to intact veined specimens of this unit. El Teniente, however, is a typical copper-porphyry system, and it is expected that the experimental results are not unique only to CMET.
The method developed for vein mapping and DVN construction limited which veins were included into the DVN. Veins with poor definition of geometry, discontinuous veins, and veins that were not identified visually were not part of the DVN. The method, however, was easy to implement, rapid, and cost-effective, providing us with an opportunity to easily construct 10 DVNs. Numerical simulations also demonstrated that the accuracy of the DVNs was sufficient for the models to reproduce key experimental behaviours and to illustrate the effects of DVN variations on specimen strength.

Measurement of specimen dilatancy and identification of various axial stress threshold levels, for example of $\sigma_{ci}$, relied on measurement of the diametric strain. The cantilever approach utilised in the loading cell posed significant limitations in terms of sensitivity and accuracy of strain measurements for intact veined rock specimens. However, use of other methods of diametric strain measurements were not possible due to the nature of the triaxial tests. Moreover, because the deformation of a veined rock specimen is subject to localised deformations along its veins, it is not certain that other methods of lateral strain measurements, especially under confined conditions, would provide better results.

The following are recognised limitations related to the numerical simulations:

- BPM models of intact material were calibrated to match the macro-scale unconfined compressive strength; the tensile strength in these models was higher than the target. This is a well-known limitation of parallel-bonded particle models, and efforts were made to mitigate its effects with the use of clumps. However, because of this limitation, use of BBM models was investigated, allowing us to develop an approach for modelling of intact veined rock. This resulted in developing an understanding of some advantages and limitations of the BBM modelling.

- Numerical models did not account for properties, such as stiffness and strengths, of individual veins. All veins were given an identical set of properties, which was an oversimplification. This approach, however, was necessary as information related to strength of individual veins was not available. Also, the experimental results showed that vein orientation with respect to the axial load played a key role in the development of fractures.
• The veins in the models had no thickness, as they were modeled using inter-particle and inter-blocks contacts. In some of the modeled specimens, Specimen 14 for example, use of this approach may have been questionable. This is a limitation of the SRM approach that had been selected for modelling of the experiments. However, the modelling results showed that this limitations was not significant.

10.7 Recommendations for Future Work

The research presented in this dissertation provides good foundation for studying behaviour of veined rock, both on the scale of intact specimens and on the scale of a rock mass. To advance our knowledge on the subject, a number of avenues can be taken. Potential work that can be carried out to extend the results of this research is discussed below.

10.7.1 Experimental Laboratory Work

This thesis focused on the behaviour of intact specimens of CMET. All specimens originated from the same area. Further experimental work should take two directions. In one, intact veined rock specimens from other parts of El Teniente and from different elevations should be carefully tested. The test results should be carefully analysed within the framework of the progressive failure of intact rock. As the second direction, veined rock from other locations needs to be studied. This will allow to determine if the results of this study are unique to CMET or ubiquitous for all intact veined rock.

In the future, selection of source material and preparation of test specimens should involve full core orientation. This will be helpful in analysing orientation of existing micro-defects with respect to the regional stress-field. It is also suggested that veined rock specimens are examined for presence of microdefects prior to testing using thin sections or possibly x-ray computer tomography (CT) scanning. Feasibility and applicability of both approaches will require evaluation.

Use and application of hyperspectral imaging should be investigated for vein mineralogy assessment. Hyperspectral imaging, being a form of non-destructive testing, can be carried out prior to compressive experiment, allowing one to establish vein mineralogy of all visible veins in the specimen prior to testing. Knowledge of vein mineralogy prior to testing will help with specimen selection and design of the experimental program.
10.7.2 Field-Scale Investigations

This research focused on the intact veined rock. In-depth investigations related to the behaviour of rock mass are needed to develop understanding how these results correlate with rock mass behaviour. The field scale investigations need to focus on in-depth rock mass characterization and field measurements of stress, deformations, and micro-seismicity. It is probably best to start such investigations on the scale of a rock pillar rather than the scale of a mine.

10.7.3 Numerical Simulations

Bonded particle DEM models have a number of advantages over bonded-block DEM models. One of them being computational efficiency and therefore speed of calculation. The limitations of the BPM models, discussed in this thesis, need to be resolved. One potential solution is to use other types of particle bonds in place of parallel bonds. For example, use of flat joint bonds (Potyondy 2014) in 3D models should be thoroughly investigated.
References


Brzovic, A. (2010). *Characterization of Primary Copper Ore for Block Caving at the El Teniente Mine, Chile*. Ph.D., Western Australia School of Mines.


Appendix A
Accounting for Veins in the MRMR Classification System

The MRMR system was specifically developed for jointed rock masses, and the addition of veins has been introduced only recently (Laubscher 1990; Laubscher and Jakubec 2001). Veins and cemented joints are treated separately in the system; they are distinguished on the basis of their scale and continuity. According to Laubscher and Jakubec (2001), veins are features of low continuity, occurring within a rock block, and cemented joints have continuity on the scale of a rock block. A rock block is defined as a finite volume of rock formed by intersecting “throughgoing” joints and potentially containing “discontinuous fractures and veinlets” (Jakubec and Esterhuizen 2007). Figure A-1 illustrates the concept of the rock mass within the framework of the MRMR system. The MRMR system is only applicable to rock masses in which veins and cemented joints are weaker than the host rock (Laubscher and Jakubec 2001).

![Figure A-1. Schematic view of the rock mass in MRMR (after Laubscher and Jakubec 2001).](image)

Based on scale, the MRMR system defines three categories of rock strength: intact rock strength, rock block strength, and rock mass strength (Figure A-2). The strength in each category is influenced by weakening factors and defects of the rock mass. Veins and their effects on the
strength are recognized on the scale of a rock block and cemented joints on the scale of the rock mass.

![Image of strength scale used in the MRMR classification](after Jakubec and Laubscher 2000).

The intact rock strength (IRS) is determined based on the results of laboratory compression tests on specimens of intact rock. Based on variations in intact strength values and relative proportions of strong and weak intact rock, average “corrected” intact rock strength is derived based on the chart and the procedure illustrated in Figure A-3.

The MRMR approach assumes that veins affect the strength of rock blocks that are making up the rock mass. Rock block strength (RBS) is calculated by degrading the average IRS value based on two conditions. For homogeneous rock blocks that are free of fractures and veins, the IRS value is reduced to 80% to account for the scale effect. For rock blocks containing fractures and veins, the IRS is reduced further, beyond the 80%, based on the intensity of vein/fracture and their infill. Veining intensity is taken into account using the linear fracture frequency (number of fractures per metre of length) approach: IRS is reduced less for blocks with lower vein frequency than for blocks with higher frequency. The effects of the infill are considered based on the hardness (based on Mohs scale) of the infilling material: harder minerals tend to reduce IRS less than softer ones. Hardness indices to the maximum of 5 are considered. To obtain the reduction factor, one multiplies the inverse of the infill’s Mohs index by the fracture/vein frequency and looks up the value from the nomogram reproduced in Figure A-4. An example of the calculation is provided in Figure A-4 (Laubscher and Jakubec 2001). Rock block strength rating is looked-up based on the chart shown in Figure A-5.
**Example**

Weak rock UCS = 80 MPa  
Strong rock UCS = 200 MPa  
\[
\frac{\text{Weak rock UCS}}{\text{Strong rock UCS}} \times 100 = 40\%
\]

% Weak rock = 52%

**Average IRS = 50% of 200 MPa = 100 MPa**

Figure A-3. Chart used to establish “corrected” IRS value with an example calculation (after Laubscher and Jakubec 2001).
Figure A-4. Nomogram relating the IRS adjustment factor to the hardness index and vein frequency used in the MRMR system with a working example (after Laubscher and Jakubec 2001).

Figure A-5. Chart used to calculate RBS rating based on RBS value (after Laubscher and Jakubec 2001).
Once the RBS and the corresponding rating are established, the effects of cemented joints can be considered. Because MRMR was designed to address jointed rock masses, open joints are considered to have the main influence on the rock mass strength.

The joint spacing (JS) rating is established based on a number of open joint sets present (maximum of 3) and the average joint spacing. The chart shown in Figure A-6 is used. The presence of “cemented joints” in addition to open ones will further reduce the strength of the rock mass. The adjustment takes place through reduction of the JS rating based on the spacing between cemented joints. The reduction factor is determined based on the chart of Figure A-7. The system stipulates that a single cemented joint set with spacing of 5 m or more or two cemented joint sets spaced approximately 0.9 m or more will not influence the rock mass rating. Closer spaced cemented joints will reduce the JS rating by as much as 70%, and the reduction percentage increases with the decreasing joint spacing. Laubscher and Jakubec (2001) consider presence of two sets of cemented joints to have less effect on the rock mass quality than of one such set. This is because the two sets are considered to be a part of a three set system, having only one set of open joints.

The effects of cemented joints on the rock mass are taken into account further through reduction of the Joint Condition (JC) rating. The JC rating (maximum of 40) is established based on large and small-scale joint expressions (e.g. amplitude, roughness), alteration, and presence of gauge. For cemented joints, the adjustment is based on the hardness of the infill material. Similar to veining, JC rating is downgraded less for infilling materials with higher Mohs indices than ones with lower indices. The adjustment factors are listed in Table A-1.

Joint spacing and joint condition ratings provide a measure of the influence of open and cemented joints on the strength of the rock mass within the MRMR system. Based on this and on the rock block strength (RBS), the rock mass strength (RMS) is calculated as a reduction of the rock block strength as follows (Laubscher and Jakubec 2001):

\[ RMS = RBS \frac{JS_{rating} + JC_{rating}}{70} \]  
(Eq. A-1)
Figure A-6. Chart for calculation of the joint set rating (after Laubscher and Jakubec 2001). Cemented joint adjustment factor is established based on spacing between cemented joints (see Figure A-7).

Figure A-7. Chart for calculation of the adjustment factor to joint set rating due to presence of cemented joints (after Laubscher and Jakubec 2001).
Table A-1. Adjustment factors used to calculate the joint condition rating within the MRMR framework for cemented joints (Laubscher and Jakubec 2001). The factor is applied to the maximum rating of 40.

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<th>Hardness</th>
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In working with veined rock masses, the main advantage of the MRMR system over other rock mass classification approaches lies in its effort to estimate and incorporate the effects of veins on the strength of the rock mass. The system’s approach in addressing effects of veins is logical and structured, and this is an advantage of the MRMR system. However, analysis of the system shows that the approach has a number of shortcomings related to its treatment of veins:

- The distinction between veins and cemented joints based on scale is ambiguous and highly subjective. This is especially true in cases where rock mass exposure is limited, for example during early stages of project development when diamond drill core provides the only means of observing rock mass conditions.
- Potential effects of veins on the strength of intact rock are not considered. The system assumes that rock specimens used in laboratory tests are free from veins.
- Vein infill material is considered to be made of a single component. In practice, vein mineralogy is more complex (Brzovic and Villaescusa 2007).

Since its original introduction, the MRMR system has become the main approach of rock mass characterization in caving projects. Publications related to its use on veined rock masses, however, are rare. In their work, Laubscher and Jakubec (2001) do not provide any information supporting how and on what basis they developed their modification to the original version of the system. It is, therefore, difficult to establish if the system is in fact effective in dealing with veined rock masses.
Appendix B
Vein Mineralogy

Mineralogical assessment of veins of the specimens tested in triaxial compression was carried out using thin sections. Fifty two thin sections were prepared from discs that were cut parallel to the diametric axes of the specimens following the experimental work. The petrographic analyses were performed by O'Shaughnessy and Mailloux (2015). Table B-1 to Table B-10 below provide a summary of vein mineralogy.

Not all veins contained in the DVNs resulted in their mineralogy being analysed. A portion of the catalogued veins was not intersected by the thin sections.
Table B-1. Mineralogical composition of mapped veins for Specimen 1.

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### Table B-7. Mineralogical composition of mapped veins for Specimen 12.

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Table B-10. Mineralogical composition of mapped veins for Specimen 19.

| Vein ID | Angle (degrees) | Thickness (mm) | Plagioclase | Anhydrite | Quartz | Biotite | Chlorite | Tremolite | Bornite | Chalcopyrite | Molybdenite | K-feldspar | Chalcocite | Magnetite | Muscovite | Volumetric Mineralogical Composition (%) |
|---------|-----------------|----------------|-------------|-----------|--------|---------|----------|-----------|---------|-------------|-------------|-----------|-----------|-----------|----------|----------|----------------------------------------|
| 1       | 87.6            | 0.73           | -           | 5         | 45     | -       | -        | -         | 8       | -           | -           | -         | -         | -         | -        | -        | 43                                    |
| 2       | 85.6            | 0.79           | 7           | 3         | 90     | -       | -        | -         | -       | -           | -           | -         | -         | -         | -        | -        | -                                    |
| 3       | 59.7            | 0.90           | 10          | 85        | 5      | -       | -        | -         | -       | -           | -           | -         | -         | -         | -        | -        | -                                    |
| 4       | 27.0            | 3.03           | NA          |           |        |         |          |           |         |             |             |           |           |           |          |          | -                                    |
| 5       | 27.9            | 1.34           | NA          |           |        |         |          |           |         |             |             |           |           |           |          |          | -                                    |
| 6       | 24.6            | 0.68           | NA          |           |        |         |          |           |         |             |             |           |           |           |          |          | -                                    |
| 7       | 35.3            | 1.07           | NA          |           |        |         |          |           |         |             |             |           |           |           |          |          | -                                    |
| 8       | 53.7            | 0.64           | -           | 5         | 95     | -       | -        | -         | -       | -           | -           | -         | -         | -         | -        | -        | -                                    |
| 9       | 29.6            | 0.45           | NA          |           |        |         |          |           |         |             |             |           |           |           |          |          | -                                    |
| 10      | 66.8            | 0.71           | NA          |           |        |         |          |           |         |             |             |           |           |           |          |          | -                                    |
| 11      | 60.1            | 1.68           | -           | 5         | 95     | -       | -        | -         | -       | -           | -           | -         | -         | -         | -        | -        | -                                    |