Experimental Investigations for SAGD Well Integrity Numerical Modelling

by

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Abstract

Steam-Assisted Gravity Drainage (SAGD), today is the most promising technique to extract heavy oil from oil sands reservoirs. Maintenance of integrity of the near-well area is crucial to help ensure that the reserves are produced properly without environmental or production optimization challenges. The research presented here addresses the geomechanical effects associated with SAGD on wellbore integrity. The output of this research can be used in coupled hydraulic-geomechanical numerical simulation platforms for assessing the possible displacement ranges of wellbores within the steam chamber in the large reservoir model.

The thesis consists of two parts. In the first part, the hydro-mechanical behavior of Wabiskaw formation located in the SAGD reservoir production site in Canada is experimentally analyzed through consolidation and steady state flow tests. Pressure-dependent compressibilities and Biot coefficients were also obtained from the caprock testing strategy. Significant 'creep' behavior was observed during the experiments process. Several possible microscale processes are presented and discussed. In the second part of the thesis the permeability of well cement is characterized through a hydraulic pulse test technique. This work includes: laboratory testing system design, construction and calibration. A parameter identification method based on a previously published analytical solution which describes the pulse behaviour was used to obtain the best estimates of the cement permeability and specific storage. The validity of the technique is evaluated with 22 separate measurements on one well cement sample. The pulse test successfully estimates the cement permeabilities which are in good agreement with steady state flow results. A simple method to interpret the pulse test data is also included in the thesis, and the results were compared with the analytical solution. Numerical simulations of the cement pore pressure decay response generated with the numerical code COMSOL are presented as well.

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List of Symbols

Α	The area of cross section of the specimen	
α	Pulse decay constant	
α_1	Porosity sensitivity coefficient	
α2	Biot coefficient	
β	Dimensionless parameter in Hsieh's solution	
C_{α}	Coefficient of secondary compression	
C_{bc}	Bulk compressibility	
C _{eff}	Effective compressibility of the specimen	
C_{pc}	Pore compressibility	
C_p	Heat capacity	
C_s	Compressibility of the minerals	
C_w	Compressibility of the water	
е	Void ratio	
Е	Young's modulus	
\mathcal{E}_{v}	Volumetric strain	
$\varepsilon_1, \varepsilon_3$	Principal strain	
ϕ	Dimensionless parameter in Hsieh's solution	
ϕ_m	Dimensionless roots in Hsieh's solution	
γ_{w}	Unit weight of water	
γ	Dimensionless parameter in Hsieh's solution	
γ_1	Pressure sensitivity coefficient	
G_s	Specific gravity	
h	Hydraulic head in the specimen	
h_d	Hydraulic head in the downstream reservoir	
h_u	Hydraulic head in the upstream reservoir	
Н	Instantaneous step increase in hydraulic head in the upstream reservoir	
k	Permeability	
k_0	Permeability at zero mean effective stress	
Κ	Hydraulic conductivity	
K_b	Bulk modulus	

Maximum bulk modulus
Solid grain bulk modulus
Dimensionless variable in Hsieh's solution
Slope of swelling line
Slope of normal consolidation line
Total length of the specimen
Dynamic viscosity of water
Modified creep index
Porosity
Porosity value at zero mean effective stress
Water content
Mean effective stress
Initial in-situ pressure
Pre-consolidation pressure
Reference pressure
Confining pressure
Pressure in the downstream reservoir
Pore fluid pressure
Pressure in the upstream reservoir
Volumetric flow rate
Over-consolidation ratio
Bulk density
Density of water
Compressive storage of the downstream reservoir
Specific storage of the specimen
Compressive storage of the upstream reservoir
Total stress
Effective stress
Principal effective stress
The time from the onset of the pulse test experiment
Temperature
Specific volume

ν_1	Poisson's ratio
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 v_{λ}, v_{κ} The value of the specific volume at the reference pressure

 V_b Bulk volume

 V_d Volume of downstream reservoir

 V_s Solid grain volume

 V_u Volume of upstream reservoir

 V_p Pore volume

x The distance along the specimen axis referenced from the downstream end

1 Introduction

Unconventional sources of petroleum have been extensively developed over the past few decades. The Alberta Energy Regulator (2015) indicates that Canada's oil sands (a water, sand and bitumen composite) are the third-largest proven crude oil reserve in the world, next to Saudi Arabia and Venezuela. Alberta, especially in the north eastern, is the main region of oil sands reserves. Alberta Energy Regulator (2015) also reports that the total remaining established reserves of in situ and mineable crude bitumen is 26.4 billion cubic metres (m³) (166.3 billion barrels). Only 5.9% of the initial established crude bitumen reserves have been produced since commercial production started in 1967. The bitumen is very viscous (viscosity > 10,000 centipoise under reservoir conditions) and has an API (air permeability index) gravity of less than 10° indicating that the natural bitumen (oil sands) is a heavy oil which hardly flows under normal temperature conditions. Oil sands production requires extensive mechanical/chemical and/or thermal input.

SAGD is the most popular technique for the extraction of bitumen in Alberta. This process involves drilling two wells, which are vertically separated by about 5 meters, that extending horizontally through reservoirs. High temperature steam is continuously pumped into the upper of these two wells which leads to heat the reservoir to temperatures as high as 250 °C as it moves into the reservoir. This thermal process reduces the viscosity of bitumen so that this heated heavy-oil will flow more freely, and it will drain by gravity into the lower well where it is pumped to the surface. An example well pair configuration is shown in Figure 1 (Tonn 2010).



Figure 1. Example SAGD Well Pair (Tonn 2010)

All SAGD exploration and production wells are drilled and then cemented to prevent unwanted fluid migration. Injection of super-heated steam into the SAGD wells and the surrounding reservoir formation cause these regions to experience specific challenges. Integrity of the near-well area is crucial for preventing leakage between geological horizons and towards the surface. This includes the integrity of the cement column, the adjacent rock, and the casing/cement and cement/rock interfaces. Production of heated reservoir fluids, injection of hot steam and relatively cold drilling and stimulation fluids create thermal stresses in the casing/cement/rock system. A complex interplay of coupled multiscale processes affects wellbore integrity. These include: elastic and plastic deformation of the rock; hardening and shrinkage of the cement; thermal stresses and deformations of casing, cement sheath and rock; poroelastic effects; and timedependent deformation. Unlike other industries, such as mining or tunneling, there is no direct surface access to the wellbore in the oil and gas industry. This makes direct field observations difficult or impossible. A combination of laboratory tests of samples from the field or analogue samples, and numerical coupling simulation enables an insight into the processes in the well casing, cement sheath and the adjacent near-well area. Our Reservoir Geomechanical Research Group (RG²) has spent a long time on investigating explicitly coupled geomechanical and reservoir simulation analysis to predict the behaviour of the SAGD wellbore. This thesis work is part of a larger research effort related to large scale coupled reservoir modelling and small scale wellbore

integrity analysis ongoing within RG². In this study the emphasis will be put on the experimental investigation of Wabiskaw formation properties and well cement hydraulic properties.

1.1 Problem Statement

Wells consist of, from the innermost surface outward: casing, cement, mud cake, and a main rock formation. During SAGD well construction and subsequent reservoir operations, these physical elements at, and in the vicinity of, the wellbore will interact in response to changes in the local stress state. The life of any well-both its longevity and its productivity reflects an integration of these various changes and the events which have caused them. According to Norsok Standard D-010 2004, well integrity is the application of technical, operational and organisational solutions to reduce risk of uncontrolled release of formation fluids throughout the life cycle of a well. Maintaining well integrity is extremely important for oil production. Loss of integrity may result in disasters like blow outs or large scale infrastructure damage. Thus well integrity should be considered and properly managed to maximize the life of the well.

Unlike other industries, such as mining or tunneling, there is no direct access to the surface of the wellbore in oil and gas industry. This makes direct field observations difficult or impossible. Coupled hydraulic-geomechanical modeling studies of the possible behaviors that may occur at the well and near the steam chamber are required for better understanding of SAGD operations. This is particularly true for effective barrier designs of a well exposed to steam simulation, especially for very shallow fields. Garnier et al. (2010) noted that conventional rules may be applied for casing design, but pioneering rules should be considered for all wellbore elements including the cement sheath and the surrounding rock formation.

During steam injection the injection well is subjected to high temperatures. Heat is conducted from the injection well through a metal casing, a grout cement annulus, and the surrounding rocks. Large scale numerical simulations provide guidance to assess the behavior of wells in the presence of a SAGD steam chamber. These simulations require a good characterization of the hydraulic and mechanical properties of the wellbore system elements (rock formation, cement annulus and steel) in order to understand well failure risk and obtain accurate well behavior predictions. The emphasis of this study is on characterizing the hydro-mechanical properties of drill cores obtained from the Wabiskaw formation and of well cement hydraulic properties. An idealized geological profile for SAGD projects in the Athabasca oil sands of western Canada is shown in Figure 2. Above most of the oil sands are the interbedded tidal-flat sequences. These are alternating thin beds of oil sands and water-saturated, clay-rich, silty mudstones. The Wabiskaw Member at the base of the Clearwater Formation includes laterally continuous sands and mudstones. These mudstones are effective barriers to steam rise. The sands are usually saturated with gas and/or water and may be bitumen-stained. The massive, laterally continuous, Wabiskaw and Clearwater mudstone provides an effective caprock. The behavior of caprock under the maximum operating pressure (MOP) at a given stage of production is dependent upon the stresses in the caprock, and the resulting deformations that will occur due to the diffusion of pore pressure and temperature. Thus it is critical to ensure that the caprock zones maintain both their mechanical and hydraulic integrity over the lifetime of a SAGD project.



Figure 2. A typical geological profile of the Athabasca oil sands (Collins et al. 2013).

When a reservoir is produced with SAGD, the low permeability of the caprock formation should ideally slow down, or ideally stop, the vertical propagation of the steam chamber in the reservoir. Therefore, effective steam containment depends on maintaining caprock integrity. Caprock failure may lead to steam release to the overlying aquifers or to surface, that can cause significant economic, environmental, and safety impacts on field production operations. Almost without exception, as part of their regulatory approval submissions all current SAGD development applications must submit an assessment of caprock integrity treating the containment of injected steam and heated reservoir fluids within the target reservoir (Alberta Energy Regulator 2016).

The impact of SAGD operations on the caprock has been assessed with different approaches: in the laboratory, with reservoir simulations, and with direct observation on SAGD projects based on monitoring data. Significant simulation work (i.e. Pooladi-Darvish and Mattar 2002; Chen et al. 2008; Le Ravalec et al. 2009) are based on the assumption that shale permeability is not affected by the stress changes resulting from SAGD. In particular, these studies disregard the geomechanical conditions (effective stress path) that the shale-barriers experienced during SAGD.

The subject of geomechanics was first used in the petroleum field to evaluate and overcome well stability and reservoir hydraulic communication problems; but it has evolved to become a part of reservoir planning and development (Santos and Ferreira 2010). Initial studies of caprock behaviour have focused on quantifying shear and tensile failure mechanisms. Many constitutive parameters have been characterized through laboratory testing (i.e. the elastic Young's modulus (E), uniaxial compression strength (UCS), friction angle and cohesion) with UCS and triaxial compression tests over the last decade. The constitutive models of caprock used were mainly based on the triaxial compression tests (i.e. Mohr-Coulomb Model). Coupled hydro-mechanical properties of caprock, however, have not been studied extensively. Despite this lack of interest, the connection between geomechanical behaviour and flow processes is critical since stress changes during the SAGD process will inevitably affect the fluid flow.

The cement sheath is a key component of any oil or gas well. In oil wells, this sheath is placed between the rock and the casing for support and sealing. It is expected to provide mechanical support to the casing string in general, while also providing both mechanical and hydraulic isolation for all of the production horizons during well construction and the operation life-of-well. This lining is subjected to various thermal and mechanical loadings during the drilling, the production, and abandonment phases (when the well must seal the subsurface from the surface). The long-term integrity of a cement sheath is the ultimate factor for determining whether the wellbore will withstand the production operations.

The cement sheath will interact mechanically with other structural elements in the wellbore region due to stresses from geological processes and operational activities. While rarely studied to date, it is vital that cement must have an impermeable matrix in order to provide zonal isolation since the effectiveness of cement as a barrier is based on its very low permeability. Interest in cement permeability has increased in the last 20 years; this is, at least in part, because the majority of wellbore failures are related to the cement job and the associated permeability distribution (Nelson et al., 2006). The knowledge of cement permeability in oil-well conditions is essential for the prediction of the well performance during the exploitation and also prediction of the sealing performance of the well. When cement is correctly placed in the wellbore and provides initially a good zonal isolation; however, this zonal isolation offen disappears overtime. In most cases this failure is caused by either the formation of a microannulus or an increase in cement permeability (Figure 3).

Permeability to liquid and gas flow is different due to viscosity differences. The permeability of set cement to liquid flow is smaller than those of the producing formations. Even seven days after the emplacement of well cements, the liquid permeability is generally too small to be measured with traditional methods like steady state method (Smith, 1990). Although cement has lower permeability values than the surrounding formations, the value of 0.1 millidarcy (md) is enough to permit an appreciable gas flow (Goode 1962). If the cement is permeable enough, fluids may percolate upwards to a shallow portion of the formation around the well and/or to the surface (Figure 4). Such a situation may cause increases in the pressure of a shallow portion of the well increasing the risk of blowouts, weaker zonal isolations, and production losses. Despite the need for quantification of cement permeabilities, laboratory measurement of low permeability materials (below 10⁻¹⁸ m²) remains a technical challenge.



Figure 3. Microannulus width equivalent to the permeability measured through the cemented annulus (Boukhelifa et al. 2004)



Figure 4. Possible fluid movement through a low permeable zone and/or through surface (Nelson et al., 2006).

1.2 Objective of Thesis

1.2.1 Laboratory Investigation of Geotechnical Properties of Caprock

The current study seeks to further knowledge of the coupled hydro-mechanical behaviour of Wabiskaw 'D' caprock from the Petro Canada Dover site by characterizing the general trend of permeability evolution on representative core samples under various isotropic stresses at constant elevated temperature. Furthermore, this research presents an appropriate constitutive model of the Wabiskaw formation generated from the isotropic consolidation experimental data. Importantly, the Biot coefficient and compressibilities which are extremely difficult to measure on the laboratory scale were obtained from current experimental strategy as well. These poroelastic parameters are required to characterize the poroelastic behavior of caprock which is more complicated than the common assumptions of linearity and isotropy. This research was conducted to provide better estimates of the stress dependence of poroelastic constants for large-scale studies, to better understand the underlying physics of the nonlinear poroelastic behavior of caprocks.

A first step for conducting large-scale SAGD well integrity analysis is to develop a proper reservoir geological model by characterizing the reservoir and overlying formations which includes basic geomechanical properties. Understanding the hydro-mechanical properties of caprock experimentally is a step towards future analysis of the reservoir on a larger scale, and would service future modelling and production of SAGD reservoirs. The thesis reports first on the experimental facilities and modifications necessary for the execution of isotropic consolidation test on Wabiskaw. Typical experimental results are then presented. A modified Cam-clay model (Roscoe and Burland 1968) which is applicable to describe the influence of volume change on bulk properties for clayey material, is extended to soil mechanical application in reservoir and petroleum engineering. Cam-clay model parameters, the slopes of the normal consolidation and swelling lines (λ and κ) in the specific volume, v versus natural logarithm mean effective stress, lnp' plot, were obtained in this current study. In the end, the study proposes an open discussion to enrich the knowledge of caprock creep behavior.

1.2.2 Permeability of Oil Well Cement

Correctly emplaced wellbore cement will initially provide good zonal isolation. The durability of cement, associated with both matrix and fluid flow properties, disappears over time.

To properly evaluate the ability of cement in retarding the transport of hazardous fluids, it is necessary to measure its permeability, which is defined as the relative ease with which fluids can move in and out of cement under imposed pressure gradients. In many cases, permeability is a more critical parameter for defining the integrity of cement than compressive strength of the cement matrix because highly permeable cement cannot effectively isolate producing zones and is more susceptible to corrosion by brines. Knowledge of cement permeability and its evolution with time is essential for optimizing well cementing design and predicting long-term well operation performance. This thesis work aims to perform transient and steady state tests on an intact oil well cement specimen. Both simple and exact analytical approaches for pulse test interpretation are needed to compare their usefulness. However, many issues may be raised in pulse decay tests resulting in main uncertainties: determination of the reservoirs storage factor, micro leakage effects and determination of the initial pulse pressure. Optimal and practical experimental strategies dealing with these factors are presented and discussed.

Under the SAGD operations, the increase in pressure and temperature alter the stress state and disturb the medium matrix, this disturbance results in pore pressure buildup within the cement sheath. As time goes on, the built-up pore pressure will dissipate, thus it's worth simulating the 1D pore pressure diffusion response in a cement sample from the pulse test with the help of finite element method.

The lessons learned from this work can upscale to reservoir scale coupled SAGD and geomechanical modelling. Both caprock hydro-mechanical parameters and cement permeability are the necessary parameters which are used as a basic input in coupled reservoir geomechanical simulations.

1.3 Structure of Thesis

The following chapter presents a short review on the challenges of ensuring caprock and wellbore integrity in SAGD operations. Chapter 3 discusses the laboratory testing conducted on Wabiskaw 'D' caprock samples. Chapter 4 presents the hydraulic pulse test conducted on intact cement sample to estimate its permeability. Finally, Chapter 5 gives the conclusions of the studies and recommendations for future research on caprock and hydraulic pulse testing.

2 Literature Review

2.1 SAGD process

During the SAGD process, steam is injected into a horizontal well within the bitumen-rich oil sands reservoir at a constant pressure. Steam migrates and condenses on the colder bitumen-rich oil sands at the perimeter of the steam chamber. As the steam condenses, the latent heat released heats the bitumen until its viscosity is reduced sufficiently to flow. The hot water and bitumen flow to the horizontal producer well by gravity alone. Production rates are automatically throttled to prevent steam production, i.e. under "steam trap" control. As a result, steam injection rates are determined by the rate at which the steam condenses under the constant pressure criterion (Collins et al. 2002).

2.2 Overview of SAGD well integrity

Steam injection is often used to increase oil production in heavy-oil fields. While the concept of thinning heavy oil by raising the temperature through steam injection is conceptually simple, the associated challenges of designing and constructing a well for this environment can be complicated.

According to Norsok Standard D-010 (2004), well integrity is the application of technical, operational and organisational solutions to reduce risk of uncontrolled release of formation fluids throughout the life cycle of a well. Maintenance of well integrity is extremely important for production of oil. Loss of integrity may result in blow outs or other large scale infrastructure damage. Thus, well integrity should be treated and properly managed to maximize the life of the well. The first step is to understand and consider all parameters involved in well design. A SAGD wellbore system mainly consists three major elements: steel casing, cement sheath and reservoir formation. The main purpose of the cement sheath is to provide effective zonal isolation for the life of the well so that oil and gas can be produced safely and economically. Most of the oil sand deposits also contain shale barriers which are supposed to impede steam rise and thereby constrain steam-chamber rise and prevent the loss of reservoir fluids to the overburden. Collectively, these shale barriers are referred to as caprock, a certain interval in the overburden rock formations above a petroleum reservoir containing the reservoir fluids within the reservoir. Sometimes, it

immediately overlies the pay zone. In other cases, there is a buffer zone between the caprock and pay zone (Yuan et al. 2013). During the production process, the wellbore is subjected to large mechanical strains imposed by temperature variations. The main issue for the well is that the large temperature increase causes the pore pressure build-up of the cement sheath and surrounding rock formation. In the meantime, wellbore thermal operations will create high stress on cement sheath and surrounding caprock formation. Therefore, well integrity is not ensured anymore. Many geomechanical/reservoir simulations were performed to illustrate the performance of well systems during the injection and production phases of SAGD treatment (i.e. Loiseau 2014). To fulfill the simulation work, reservoir formation and cement properties are the necessary inputs. Chemical stability, mechanical properties, and permeability are commonly the main parameters determined for characterizing oilfield well systems. The knowledge of these properties is often enough to estimate whether a wellbore system will maintain long-time integrity. In this research, the caprock, Waibiskaw formation and cement annulus is the focus.

2.2.1 Caprock Integrity

The thermal recovery of bitumen reservoirs by SAGD is often designed to maximize the operating pressure while maintaining a safe and economic operation. The limitation of the maximum operating pressure is based on maintaining caprock integrity. Caprock contains steam and fluids within the reservoir; therefore, understanding the integrity of the caprock over the life of the operation is critical in order to ensure a safe and economically viable project. The two requirements to ensure caprock integrity are: that there is an adequate hydraulic seal, and that the seal remains mechanically intact (Collins 2007). An adequate hydraulic seal is one that will maintain a sufficiently low permeability to ensure that reservoir fluids are contained over the life of the project. The description "mechanically intact" requires that the caprock has adequate strength and deformation properties to withstand the pressures, temperatures, and deformations imposed by the SAGD process. Figure 5 is a schematic of a SAGD pattern of 10 well pairs and explains why caprock could be severely stressed and may eventually limit the injection pressure of SAGD operation. Changes in the *in-situ* stresses that cause dilation and contraction within caprock formation are due to the combined effects of reduced and enhanced effective stress due to high-pressure steam injection and oil production. Most reservoir engineers presume that the permeability of the caprock is fixed and is independent of operating pressure. This is not true because the contraction and dilation in the caprock layer according to the pressure change will affect the shale formation's porosity and absolute permeability (Collins 2007). These changes are expected to be permanent and irreversible.



Figure 5. Shear, dilation, and heave associated with SAGD (Collins 2007)

Over the past decade, after Joslyn Creek steam release accident (ERCB 2010), an increasing awareness of the geomechanical behaviour of caprock has resulted in more extensive geomechanical testing of caprock core specimens. These tests are primarily aimed at quantifying shear and tensile failure mechanisms of caprock, and measuring material properties, like compressive strength and Young's modulus, of these materials. In fact, a good and comprehensive geomechanical simulation of the reservoir and caprock behaviour should incorporate the variation of hydro-mechanical properties of these rocks. The most common material properties used in these numerical simulators are the compressibility, the permeability and the constitutive model for reservoir geomechanical analysis. Several distinct compressibilities are used: rock compressibility, bulk compressibility, pore compressibility and grain compressibility. For caprock these material properties are not constant but vary with the magnitude of the effective stresses in the formation. In addition, these properties will vary depending on the stress path followed in the formation.

Researchers before have spent long time analyzing the variation of oil sands properties under simulated reservoir conditions and trying to include this relationship into the simulation work. For example, Collins et al. (2002) integrated a relationship between the volumetric strain and the absolute permeability using an effective stress approach in order to model oil sands behaviour. The encroaching steam chamber was found to modify the stress regime, which in turn modified the permeabilities within the reservoir. This analysis is based on the existing laboratory data on quality specimens of non-bituminous Athabasca oil sands. However, there is a big knowledge lag on caprock formation quantitative characterization. Currently, to the author's knowledge, there is no published data related to Alberta's caprock properties variations like permeability, porosity and poro-elastic properties. Also, there is no consensus constitutive model that describing caprock formation in the geomechanical analysis. Therefore, efforts are required to investigate the hydro-mechanical properties of cparock. In Chapter 3, the experimental program focused on Wabiskaw 'D' caprock properties characterization is presented, which is going to fill the knowledge gap in this area.

2.2.2 Cement Annulus Integrity

The main purpose of cementing oil wells is to maintain proper hydraulic isolation among the various permeable layers. Best practices used to promote a competent hydraulic sealing of the annulus during primary cementing (cement/spacer design considerations, casing centralization, pipe movement, mud removal techniques, etc.) are all well documented (Suman and Ellis 1977; Nelson 1990; Worldwide Cementing Practices 1991). Once the cement is in place and set, continued annular isolation depends upon the stability of the mechanical and hydraulic properties of the cement. These properties will be affected by changes to the stress conditions which the cement sheath is placed. For many years the petroleum industry has recognized the problem of gas or fluid invasion of wellbores after cementing. Cement breakdown, typically caused by shear failures and/or chemical degradation, is the most likely source of failure in a steam injection well, (Figure 6). Post-cementing stress imposed upon the sheath can result from a change in the pressure environment (stimulation treatments, mandated casing pressure testing, change of wellbore fluid properties, reservoir depletion/injection, etc.) Tectonic forces and formation compaction during production can also be responsible for changes in the wellbore stress field (Mueller et al. 2004). Stress and temperature changes during operation can result in deformation of existing SAGD wells and unwanted interaction between casing and cement or cement and formation. This cement failure can lead to steam injection migrating to zones other than the injection target zone, loss of production, communication of corrosive fluids in the annulus, casing movement, and other associated problems. In fact, any type of fluid channel in the cement sheath is a very undesirable occurrence during well operation.



Figure 6. Cement-sheath damage (Bois et al. 2011)

Integrity of the cement sheath is essential for maintaining robust well conditions during reservoir production cycles. In the conventional analysis, chemical stability, compressive strength, and permeability are commonly the main parameters determined for oilfield cement. In this study, the focus is mainly on the cement permeability measurement. Recently to fully characterize the insitu cement system, Garnier et al. (2007) suggested tensile strength, elastic properties and failure criterion should be added as well. In this study, permeability measurement of set cement is the focus. Permeability of cement to liquid and to gas is two different properties. However, the set cement liquid permeability is smaller than those of the producing formations. Even seven days after the placement of cement in place, the liquid permeability is generally too small to measured (Smith, 1990). The ways for measuring cement permeability documented in the literature is further explained in Chapter 2.3.

There are many different phenomena to let the gas/fluid pass through the cement such as wrong cement density, poor mud/filter cake removal, premature gelation, excessive fluid loss, high permeable slurry, high shrinkage, cement failure under stress and poor interfacial bonding. These factors are depicted in Figure 7. Although gas/fluid may enter the annulus through cement by many different mechanisms, it needs a driving force to initiate the fluid flow and space in the cemented portion of the annulus to occupy (Bonett, A. et al., 1996).



Figure 7. Cement-sheath damage (Bonett, A. et al., 1996)

When high temperature steam was injected into the well, the cement sheath could be regarded as fluid-saturated porous medium experiencing heating process, both the pore fluid and the solid matrix expand. Due to the higher thermal expansion of fluid (Butler 1986), excess pore pressure is induced in this process, which may lead to the development of effective tensile stress and fracturing or failure of structure. Another is related to the pore pressure buildup, which may be induced by thermal effects. When the time-scale of pore pressure diffusion is much greater than that of thermal diffusion, the time response of rock is mainly controlled by its permeability and bulk compressibility. For analyzing one-dimensional pore pressure diffusion, hydraulic pulse test is deemed as the best technique for simulating the hydraulic behavior of well cement. In terms of the permeability of cement, which is directly related to the hydraulic integrity of the wellbore, four factors are largely related, chemical composition, the fineness to which the cement is ground, the amount of mixing water used (the permeabilities of all cements increase with the water-cement ratio), the procedure used in mixing the slurry, cement curing conditions and testing conditions. In other words, because of different operation strategies in the field, it's hard to estimate the real insitu permeability and other hydraulic properties of well cement. For decades, different types of

well cement permeability were measured to simulate the in-situ wellbore sheath response, however, because of the factors mentioned above in each study is all different, it's difficult to compare the results and make a conclusion for well cement permeability. For pore pressure diffusion response, only Selvadurai and Carnaffan (1997) did hydraulic pulse experiment on cement grout to analyze the permeability but in the radial direction.

2.3 Permeability Measurement

Scientists and engineers have developed a variety of techniques for the measurement of properties related to permeability. In principle, there are two laboratory testing techniques, based on steady state or transient methods, which can be used to measure the water permeability. The steady state method monitors a steady flow through saturated specimens under a constant pressure head and permeability is calculated directly from Darcy's law, (e.g. Banthia and Mindess 1989; Hooton and Wakeley 1989; Ye 2003). The second method is an indirect test method, which utilizes a transient flow approach and is commonly referred to as a hydraulic pulse test. Rocks with high permeability can easily be measured by the former technique while it is more convenient to use transient methods for low permeability rocks.

2.3.1 Steady state method

Laboratory determination of permeability with the steady state method usually involves either the application of a constant pressure gradient across a sample and the measurement of the steady-state flow rate, or the application of a constant flow rate through the sample and the measurement of the corresponding steady-state pressure drop. The main advantage of steady state tests is its simplicity of analyses: the permeability of the porous material can be calculated from knowledge of the area over which the flow takes place, the hydraulic gradient, and the volume flow rate through the sample. The primary disadvantage of the steady state technique is that for materials with very low permeability, such as cement, long periods of testing are necessary to achieve a steady state flow.

The constant pressure gradient test is used more frequently than the constant flow test, but both techniques have their advantages (Selvadurai and Carnaffan 1997). In this thesis we applied constant pressure gradient approach so it its discussed in more detail here. Constant pressure gradient experiments are performed by fixing a different pore pressure between the upstream and downstream while monitoring the flow rate. The measured flow rates were used to calculate the permeability using Darcy's equation:

$$Q = \frac{kA}{\mu_w} \frac{\Delta p}{l}$$
 Eq. 1

where Q (m³/s) is the volumetric flow rate across the sample cross-section area; μ_w (Pa·s) is the dynamic viscosity of fluid; $\Delta p/l$ is the pore pressure gradient across the specimen; A (m²) is the area of cross section of the specimen and k (m²) is the average permeability.

The most common configuration for the steady state testing of permeability involves the one-dimensional testing of a specimen with rectilinear laminar flow (Selvadurai and Carnaffan 1997). Steady state tests are used when steady flow rates can be established within a reasonable time frame; for example, when tests are conducted on rocks with permeability in the range $10^{-14} - 10^{-18}$ m² (see e.g. Heystee and Roegiers 1981; Tidwell and Wilson 1997). The second disadvantage of a steady state test is that the flow rates generated even under extremely large pressure gradients can be extremely small in low permeability materials. Accurate measurement of such low flow rates can be extremely difficult owing to instrument error.

2.3.2 Hydraulic pulse test

The use of hydraulic pulse tests was pioneered by Brace et al. (1968) and has been successfully used to estimate the permeability of most low permeability geomaterials such as cement pastes (Roy et al. 1993; Selvadurai and Carnaffan 1997; Scherer et al. 2007). Most commonly, this test consists of applying a sudden pressure pulse on the upstream end of a sample and measuring the pressure-time histories in the upstream and downstream reservoirs. These pressure responses allow for the assessment both of the intrinsic permeability and of specific storage. The latter is defined as a unit volume of saturated aquifer releases from storage when exposed to a unit decline in average head (Hantush 1964). During a pulse test, the intrinsic permeability governs the transient evolution whereas the specific storage governs the final equilibrium pressure. The reason that the hydraulic pulse test cannot be regarded as method for directly determining permeability is because interpretation of the measured pressures requires other material and physical properties of both the porous medium and the permeating fluid. These

include: the porosity of the connected space in the porous medium; the compressibility of the porous skeleton and the solid material constituting the skeleton; the compressibility of the permeating fluid; and its dynamic viscosity. This is in contrast to the steady state hydraulic tests that require only knowledge of the physical dimensions of the flow region, the associated boundary conditions and the flow rates established during steady state flow.

A transient one-dimensional flow technique for determining permeability was first introduced by Brace et al. (1968). The tests were performed on cores of Westerly granite under high confining pressures (Figure 8). The upstream and the downstream reservoirs are filled with water. The pressure cell is also filled with water and pressurized to a constant value. After sample saturation and pore-pressure homogenization, the pressure in the upstream reservoir P_u is suddenly increased. The evolution of the pressure in the upstream and downstream reservoirs is then measured. Figure 9 illustrates the principle underlying the test. Since the test specimen is within a closed system, the pressure in the downstream reservoirs. During the test, only one physical quantity, pressure or pressure decay is measured. Pressure, unlike flow rate, does not need to be integrated over some time period and can be measured continuously in real time with high-precision electronic transducers. Also the rate of propagation of a pressure pulse is often much faster than the rate of propagation of a compositional flow. This feature makes it possible to successfully conduct experiments on low-permeability specimens much faster than by using the conventional constant-head methods.



Figure 8. The confined sample arrangement used in transient pulse test (after Brace et al. 1968)



Figure 9. Principle of the pulse test



Figure 10. Schematic diagram with initial and boundary conditions for transient pulse permeability test (Hsieh et al. 1981)

The exact solution considering sample's storage effect to the transient-pulse decay was derived by Hsieh et al. (1981) with a companion graphical method proposed by Neuzil et al. (1981). Their approach allows both the permeability and the specific storage of a specimen to be determined. A schematic diagram illustrating the initial and boundary conditions for the transient-pulse permeability is depicted in Figure 10. One-dimensional transient flow of a compressible fluid through a saturated porous and compressible medium can be described by the following differential equation, and associated boundary conditions, which combines the principle of conservation of fluid mass in a deformable matrix and Darcy's law for laminar flow through a hydraulically isotropic matrix:

$$\frac{\partial^2 h}{\partial x^2} - \frac{s_s}{\kappa} \frac{\partial h}{\partial t} = 0, \text{ for } 0 < x < l \text{ and } t > 0$$

$$h(0, t) = h_d(t), \text{ for } t \ge 0$$

$$h(l, t) = h_u(t), \text{ for } t \ge 0$$

$$h_u(0) = H$$
Eq. 2

where h, (or more specifically h(x, t)), is the hydraulic head in the specimen (dimension l), h_d and h_u are the hydraulic heads in the downstream and upstream reservoirs, respectively (m), x is the distance along the specimen axis referenced from the downstream end (m), t is the time from the onset of the experiment (s), H is the instantaneous step increase in hydraulic head (m) in the upstream reservoir at t = 0, A is the cross-sectional area of the specimen (m²), l is the total length of the specimen (m), S_d and S_u are the compressive storages of the downstream and upstream reservoirs, respectively (m²), defined as the changes in fluid volume in the reservoirs per unit change in hydraulic head in the reservoirs, and *K* and *S_s* are the hydraulic conductivity (m/s) and the specific storage (m⁻¹) of the specimen, respectively. The relationship between hydraulic conductivity *K* and permeability *k* is described in following:

$$K = \frac{\gamma_w}{\mu_w} k$$
 Eq. 3

where γ_w is the unit weight of the water (kg/m³), μ_w is the dynamic viscosity of water (Pa·s).

The specific storage S_s , is a function of the compressibility of the pore fluid, the bulk and matrix compressibilities, and the interconnected porosity of the specimen as shown in the relation:

$$S_s = \gamma_w [nC_w + C_{eff} - (1+n)C_s]$$
 Eq. 4

where C_w is the compressibility of the fluid (Pa⁻¹); C_{eff} is the effective or bulk compressibility of the sample (Pa⁻¹); C_s is the compressibility of the minerals in the sample (Pa⁻¹); n is the porosity of the sample.

The mathematical analysis developed by Brace et al. (1968) for the transient pulse test assumes that there is no compressive storage in the specimen, in which case the second term in Eq. 2 vanishes, and become:

$$\frac{\partial^2 h}{\partial x^2} = 0$$
 Eq. 5

The solution for the pressure at the upstream reservoir decays exponentially as a function of time:

$$P_u(t) - P_d(t) = (P_u(t_0) - P_d(t_0))e^{-\alpha t}$$
 Eq. 6

and

$$\alpha = \frac{kA}{\mu_w C_w l} \left(\frac{1}{V_u} + \frac{1}{V_d} \right)$$
 Eq. 7

where $P_u(t)$ and $P_d(t)$ are the upstream and downstream pressures at time t, $P_u(t_0)$ and $P_d(t_0)$ are the initial upstream and downstream pressures respectively, μ_w is the water viscosity (Pa·s); k is the permeability (m²), A is the cross-sectional area (m²), l is the length of sample (m), C_w is the water compressibility (Pa⁻¹), V_u and V_d are volumes of upstream and downstream reservoir (m³). The decay constant α can be directly determined from the slope of the line when plotting the pressure decay $P_u(t) - P_d(t)$ on semi-log paper against time This interpretation is still relevant and popular nowadays for its simplicity of use (Chenevert et al. 1993; Kwon et al. 2001). This validity of this method is a best if the testing fluid is water or liquids that can be treated as incompressible fluid in the testing pressure range.

The solution of the full transient-pulse test incorporating both the specific storage and the hydraulic conductivity (as described by Eq. 2) was pioneered by Lin (1977) using a finitedifference approach. Although the forms of his expressions are slightly different from those presented in this chapter, the concepts are substantially similar. In the Lin (1977) analysis, the specific storage of the specimen could be assumed with its magnitude determined independently by measuring the porosity and the bulk and matrix compressibilities of the specimen. Lin (1982) later did parametric analyses of the transient method to compare his numerical approach to the original method of Brace et al. (1968), and limitations identified in the numerical method were expounded upon. More restrictive analytical solutions of the differential equation has been derived by Hsieh et al. (1981) given by Eq. 8. The solution here is in the form of pressure evolution instead of hydraulic head for practical illustration in the experiment.

$$\frac{P(x,t)}{\Delta P_0} = \frac{1}{1+\beta+\gamma}$$

$$+ 2\sum_{m=0}^{\infty} \times \frac{exp(-\alpha\phi_m^2)[\cos\phi_m\xi - (\gamma\phi_m/\beta)\sin\phi_m\xi]}{(1+\beta+\gamma-\gamma\phi_m^2/\beta)\cos\phi_m - \phi_m(1+\gamma+2\gamma/\beta)\sin\phi_m}$$
Eq. 8

where ξ and α is a dimensionless variable, β and γ are two dimensionless parameters:

$$\xi = \frac{x}{l}$$
 Eq. 9

$$\alpha = \frac{Kt}{l^2 S_s}$$
 Eq. 10

$$\beta = \frac{S_s A l}{S_u}$$
 Eq. 11

$$\gamma = \frac{S_d}{S_u}$$
 Eq. 12

and ϕ_m are the roots of:

$$\tan \phi = \frac{(\gamma + 1)\phi\beta}{\gamma\phi^2 - \beta^2}$$
 Eq. 13

The solutions for dimensionless hydraulic heads at the specimen ends, x = 0 and l, were used by Hsieh et al. (1981) to evaluate the response characteristics of the hydraulic heads at upstream and downstream reservoirs in various simulated cases. Dimensionless forms of β and γ were used as variables. Incorporating the same expressions, Neuzil et al. (1981) proposed a graphical method based on matching dimensionless type curves to determine both the hydraulic conductivity and specific storage of the specimen from the transient-pulse records. The solutions of Hsieh et al. (1981) were also used by Wang and Hart (1993) for error estimations of hydraulic properties obtained from the transient pulse test. Sensitivity coefficients for hydraulic pressure with respect to the hydraulic conductivity and specific storage were proposed to optimize experimental design. Although graphical solutions make it possible to evaluate both the permeability and the specific storage of a rock specimen, the attendant procedures are relatively complicated. Most investigators use the analysis of Brace et al. (1968) to interpret their experimental results, although this analysis was developed for rocks with negligible porous and pore compressibility. As such, this analysis will only provide an estimate of permeability. Consequently, this solution is best suited for testing rock specimens having a negligible compressive storage like some crystalline rocks. It is generally poor for rocks such as shales and mudstone, which have significant porosity and compressive storage. The specific storage is an equally important hydraulic property associated with transient flow processes in porous media. For a typical reservoir-specimen-reservoir experimental system employed in a transient pulse test, the validity of using the simplified solution to calculate the permeability is affected directly and constrained by the ratio of the compressive storage in the rock specimen to that in the reservoir system (Neuzil et al. 1981; Lin 1982; Zhang et al. 2000), thus leading to the importance of the determination of S_u and S_d in one spefiic testing system.

Before performing transient pulse tests, calibration tests must be conducted to determine the dead volumes (V_u and V_d in Eq. 7) and reservoir compressive storage (S_u and S_d in Eq. 12), of the permeating system. The dead volumes are important parameters for using Brace's method, while the reservoir compressive storage corresponding to water volumes required to increase pressure within the reservoir are important factors for Hsieh's solution. One good way to measure the volumes of the reservoir is using a technique based on the PVT properties of nitrogen and introducing an accurately known volume change to the system whilst measuring the associated

pressure changes at constant temperature. The detailed procedure of this method was documented in Zhang et al. (2013). This thesis adopted the similar methodology for measuring V_u and V_d with minor adjustment, which is elaborated in Chapter 4.2.3.1.

As emphasized by Hsieh et al. (1981), the compressive storage of the fluid reservoir is the sum of two effects: the compressibility of the fluid in the reservoir and the deformation of the reservoir, which includes all the associated tubing and instrumentation. In general, S_u and S_d must be determined experimentally. The calibration method used in this study is similar to the method used by Olsen et al. (1988) for determining the compliance of the permeant system in a constant-flow permeability test: the compressive storage of the system is the amount by which the volume of liquid contained within the system (dV) varies in response to a change in the head within the system (dh). It follows that the compressive storage can be measured by using the syringe pump to introduce liquid into the system at a constant rate, Q = dV/dt, while monitoring the consequent head variation with time, dh/dt, with the transducers. The compressive storage of the system is

$$S_{u/d} = \frac{dV(cm^3)}{dh(cm H_2 O)} = \frac{\frac{dV}{dt}(cm^3/s)}{\frac{dh}{dt}(cm/s)}$$
Eq. 14

where 1 cm $H_2O = 98$ Pa. The test data in Figure 11 are then introduced into Eq. 14, the compressive storage of the system could be calculated. They also presented that the compressive storage measured as a function of the pressure in the system. The storage decreases with increasing pressure and approaches a constant value asymptotically. The minimum value is the storage of the system when it is fully saturated and free of undissolved air. Boulin et al. (2012) also noticed the exponential evolution of the reservoir compressibility with pressure. They explained this phenomenon as being due to the dissolution of highly compressible air into the water at high pressures. The phenomenon can be mitigated by evacuating the air from the system prior to saturation. This evacuation is extremely important for the pulse test execution, since it's going to affect the value of S_u and S_d , the necessary known parameters for permeability interpretation from the data using Hsieh's mathematical model.


Figure 11. A typical compressive storage measurement (Olsen et al. 1981)

3 Laboratory Investigation of Wabiskaw 'D' Caprock Hydromechanical Behavior

Abstract: Reservoir geomechanical simulation of steam assisted gravity drainage (SAGD) caprock behaviour is of great importance in the analysis of wellbore integrity for safety operations in the field of petroleum extraction. In this section, results from a hydro-mechanical investigation of materials from the Wabiskaw 'D' formation are presented. Methods of measurement as well as experimental devices are illustrated. Laboratory experiments showed that, due to the low permeabilities (smaller than 10^{-18} m²) of shale, the consolidation effects of Wabiskaw caprock may act on a time scale of days. At a sustained total stress, these samples exhibit pronounced creep-like behavior. Microscopic evidence suggests that changes to the microscale components and structures of the clay-shale may be responsible for this behaviour. An open discussion is given in this chapter which provides several possible processes and factors causing such macroscopic behavior. Additionally, the impact of this behaviour on wellbore integrity is discussed. The non-linear volume reduction during the isotropic consolidation test allows the estimation of pressuredependent compressibility and Biot coefficient of the material as well. The evolution of permeability in Wabiskaw shale during application of isotropic confining pressure was measured by conducting one-dimensional constant pressure steady state tests. These tests were carried out during loading-unloading cycles. Irreversible permeability changes occurred. Mathematical relationships that used to describe natural rocks are applied here for describing the evolution of stress path dependent permeability. The outcome of this study provides necessary hydraulic and mechanical properties for coupled hydro-mechanical modeling of caprock behaviour during bitumen extraction.

3.1 Introduction

The Wabiskaw-McMurray bitumen deposits located at northeastern Alberta represent a significant oil reserve which has been extensively exploited in recent decades. The recovery has been greatly enhanced through the use of SAGD. During the SAGD process the bitumen is rendered more mobile through by the injection of high temperature steam. This process increases the reservoir pressure which, in turn, alters the local stress state and induces deformations in the caprock overlying oil-sands reservoir. The caprock is defined as a succession of low-permeability

and geomechanically strong strata that can effectively contain injected steam and heated reservoir fluid (Alberta Energy Regulator 2016). A better understanding of the behaviour of caprock materials during the SAGD related deformation is essential in designing optimal stimulation strategies. Loss of caprock integrity surrounding the wellbore by these changes may lead to the release of steam into overlying aquifers or to the surface, causing significant economic penalties, as well as environmental and safety impacts on field production. The most striking example of this occurred in May 18, 2006 at the Joslyn Creek site (ERCB Staff Review and Analysis 2010). Experimental studies related to the hydro-mechanical behavior (permeability and deformation) of the caprock can provide insights into critical data for constitutive models and for caprock mechanical and hydraulic integrity analysis over the lifetime of a SAGD project.

SAGD operations are highly dynamic. Conventional reservoir simulations of thermal recovery processes in heavy oil do not explicitly incorporate geomechanics. When geomechanical processes are considered, caprock properties cannot be considered to be unchanging. The effects associated with changing reservoir pressures on the caprock overlying the reservoir should be characterized, and in turn, these values should be used to modify the reservoir parameters.

During reservoir production cycles, caprock deformation will be induced due to changes to the virgin in-situ stresses. These deformations will cause the porosity, permeability of caprock to evolve as the project develops. During this project's literature review a lack of data on intact caprock permeability, and on how it evolves with effective stress from Alberta's oil sand deposits, was clearly noted. The characterization of caprock hydro-mechanical properties under specified stress paths through experimental measurements should be incorporated into geomechanical research programs dealing with the short and long term integrity of SAGD operations.

The main objectives of the present experimental study are to investigate the deformation of Wabiskaw 'D' caprock under isotropic consolidation conditions and to measure the stressdependent permeability in two caprock samples undergoing consolidation-type loading paths. A constitutive model that describes the stress-strain behavior of Wabiskaw is proposed which has a quantitative agreement with experimental data. Approximate relationships proposed in the literature for natural rocks are applied here to describe the isotropic stress state-dependent evolution of the permeability characteristics of these samples. A steady-state permeability measurement is done on the caprock sample while holding the confining pressure fixed. This involves determining the flow rate of water achieved after applying a constant differential pressure across the sample. The parameterization of permeability with effective stress is important to fully couple the effect of deformation and fluid transport in this, and all other caprocks. Additionally, poroelastic properties were obtained during the experimental investigation of the sample bulk and pore volume changes. Of specific interest are the Biot coefficient and compressibility coefficients proposed by Zimmerman (1986).

Creep deformation is specifically discussed, however more comprehensive creep testing programs are required to fully characterize the long-term behavior of the Wabiskaw samples, since it will not only affect the deformational response of the caprock formation, but also because this type of plastic deformation can alter the state of stress around the wellbore.

3.2 Experimental Investigation

3.2.1 Sample Description and Sample Preparation

The Wabiskaw Member is a clay-rich, low permeability, shale at the base of the Clearwater Formation and conformably overlies McMurray formation. The Wabiskaw Member is subdivided into four units denoted, from top to bottom, by A, B, C, D. As a transitional buffer zone from the reservoir upward to the Clearwater, the Wabiskaw member also contains sandy facies that can be bitumen-saturated (Yuan et al. 2013). Usually the Wabiskaw unit and the Clearwater shale make up the potential caprock layers. However, the reservoir material, the McMurray Sand, also plays an integral role in determining the deformations and stresses transferred to the caprock. Therefore, both reservoir and caprock materials require detailed representation in engineering calculations or modeling exercises pertaining to caprock stability. In this study, the tested core was from the well 1AA/04-32-92-12W4M within the site of Petro Canada Dover SAGD project. It was taken using a PVC core liner from a depth of about 106 m.

In-situ well log analysis and the general stratigraphic column in the vicinity of the well is presented in Figure 12. Vertical stress was estimated by integrating the density log, and the volume of shale was determined from the γ -ray log. Figure 13 illustrates the core interval and the location of two test specimens used in this study. In order to minimize disturbance of the sample, and to

preserve in situ lithologic features, the core sample was wrapped with preservative film, sealed in an aluminum tube shortly after recovery. Subsequently, it was carefully transported to the laboratory and stored in humidified condition prior to the test, and experiments were performed under drained condition. From the preliminary observation of the sample, it is weak and soft with many chips and flaws.



Figure 12. Log for Well 1AA/04-32-92-12W4M and stratigraphic column



(b)

Figure 13. Photos of Wabiskaw core interval used in selecting caprock specimen

The clay shale core was weak, soft and generally fissured. Extensive specimen preparation with specific attention to the pre-existing fractures, was required prior to the testing. Two specimens, 6 in long, were first cut from the tube and then following ASTM D4543-08, the samples were then trimmed to cylindrical specimens using the lathe shown in Figure 14 (approximately 2.5 in diameter, 5 in long). The lathe allowed regular and parallel top and bottom faces to be obtained. Two samples will be denoted as 1st Sample and 2nd Sample, respectively throughout this chapter. Each sample was weighed, and height and diameters (top, middle, and bottom) were measured. Specific gravity (G_s), was estimated by means of the pycnometer method including thoroughly crushed the sample in a mortar following ASTM D854-14. The water content (w) was estimated by oven-drying the sample at 80 °C and monitoring bulk weight loss due to evaporation of pore water. Bulk density (ρ_{bulk}) was determined from measurements of mass and volume of cylindrical specimen. The initial void ratio and porosity of each specimen was derived from measured properties following Eq. 15 and Eq. 16. The specimens' basic geotechnical information are summarized in Table 1.

$$e = G_s(1+w)\frac{\rho_w}{\rho_{bulk}} - 1 Eq. 15$$

$$n = \frac{e}{1+e}$$
 Eq. 16



(a) 1st Sample (b) 2nd Sample

Figure 14. Prepared cylindrical specimens: (a) 1st Sample; (b) 2nd Sample

	1st Sample	2nd Sample
Diameter (mm)	64.01	63.49
Length (mm)	114.19	122.69
Specific Gravity	2.57	2.70
Water Content (%)	13.80%	14.60%
Bulk Density (kg/m ³)	2112.75	2010.86
Void Ratio	0.374	0.527
Porosity	0.272	0.345

Table 1 Geotechnical information of two samples

The mineralogical composition of the Wabiskaw 'D' caprock was determined by means of X-ray diffraction (XRD; see Table 2). The sample consists mainly sand/silt sized grains (>80%) with less than 20% in the clay sized fraction. The coarse fraction is predominately quartz, with feldspars and non-swelling clay minerals contributing virtually all of the remainder. The clay-sized fraction is kaolinite rich, with less significant amounts of illite and smectite. Dolomite was the

only identified carbonate in the sample. Interestingly 1st Sample contains as high as twice the clay content than the 2nd Sample, this result confirms the heterogeneity of Wabiskaw formation in the field. The microstructure of the fresh core (see Figure 15) and the post-consolidation material were analyzed with scanning electron microscopy (SEM). The fresh samples contain very fine sand particles extensively surrounded by clay minerals (Figure 16). Apparently from the initial observation, we could also find that the 1st Sample has relative bigger clay content than the 2nd Sample. This difference is the primary reason causing different macroscopic behavior between these two samples later presented in this chapter. In addition, the inter-granular porosity in both samples is extensively plugged with clays and minor amounts of koalinite structures are noticed (Figure 17).

	TYPE	WEIGHT	CLAYS				Total					
SAIVIFLE	OF ANALYSIS	%	Qtz	Plag	K-Feld	Dol	Pyr	Kaol	Chl		Smec	Clay
1	BULK FRACTION:	70.97	74	2	2	5	2	4	2	5	3	14
106.25m	CLAY FRACTION:	29.03	6	0	0	0	0	20	18	33	23	94
	BULK & CLAY	100	54	2	2	3	2	9	7	12	8	36
2	BULK FRACTION:	87.46	74	3	5	3	1	5	0	8	1	14
106.32m	CLAY FRACTION:	12.54	8	1	0	0	0	44	6	22	19	91
	BULK & CLAY	100	65	3	4	3	1	10	1	10	3	24

Table 2 Mineralogical composition of the investigated material



(a) 1st Sample



Figure 15. SEM microphoto showing sand particles surrounded with clay minerals: (a) 1st Sample; (b) 2nd Sample



(a) 1st Sample cuttings



(b) 2nd Sample cuttings

Figure 16. Original sample cuttings: (a) 1st Sample cuttings; (b) 2nd Sample cuttings



Figure 17. SEM microphoto showing Kaolinite structure

3.2.2 Experimental Set-Up

The experimental setup consists of a pressure cell, a heating chamber and three syringe pumps. The apparatus used in this research is a carefully designed triaxial cell called NMC 6500, equipped with steel walls in order to sustain relatively high confining pressures of the order of 45 MPa. Figure 18 presents a cross section of the hydrostatic cell. The cell was developed in Geomechanical Reservoir Experimental Facility (GeoREF). The detailed design and function information about this cell was illustrated in Deisman et al. (2011). The cell is capable of testing cores having 2.5 in of diameter at cell temperatures as high as 60 °C. The system is fully computer-controlled in both data acquisition and loading programme. A schematic diagram of the experimental system used in this study is shown in Figure 19; Figure 20 and Figure 21 are photographs of the experimental setup in GeoREF. The apparatus operates at elevated temperatures (40-60 °C) and is designed to independently control the hydrostatic confining pressure and pore pressure at the top and bottom of the sample. Teledyne ISCO pumps (260D-series, with maximum operational temperature and pressure of 40 °C and 50 MPa respectively) were used for all of the fluids. The downstream, upstream pore pressure and confining pressures are generated by pumps A, B and C (Figure 19) respectively, which are controlled by a single

pump controller unit. The pumps can send fluids at a designated flow rate or work to maintain a designated fluid pressure. Stainless steel Swagelok fittings and tubing were used for all pressured lines. Silicon oil is used as the confining pressurizing fluid for application of high cell pressure, while distilled water is used as the pore fluid for simulation of the SAGD steam injection.

Any change in the ambient temperature can affect the estimation of permeability i.e. any changes in temperature can induce excess pore pressure in the sample due to the differential thermal expansion of the pore water and the porous skeleton and affect the flow rate through the specimen. In order to minimize the influence of temperature change on the results of the permeability tests, most of the system is isolated in a heating chamber unit which maintains a constant temperature to ± 0.05 °C. For the current test, the oven temperature was set at 40 °C; this was monitored with several thermocouples placed in the fluid lines. Constant temperature in the oven helps the pump moves smoothly and monotonically, providing accurate volumetric information. The sample volume change is inferred from the back pressure pump, the amount of water withdrawn from or supplied to the sample in order to maintain a constant pore pressure while the rock is deforming under constant confining stress. Three Honeywell pressure transducers with a range of 50 MPa are placed between the pump and the cell, measuring the pressure with an accuracy of 0.25%. The temperatures, cell pressure, upstream and downstream pressure readings are continuously recorded using Keysight 34972A Data Acquisition/Data Logger switch unit which was programmed to collect all the analogues signals from the pressure transducers and thermocouples and store the converted digital values.



Figure 18. Cross section of the high-pressure hydrostatic cell



Figure 19. Schematic diagram of the experimental design



Figure 20. Photograph of the experimental equipment inside oven



Figure 21. Photograph of the experimental equipment outside oven

3.2.3 Experimental Procedure

The experimental campaign reported in this chapter consists of high-pressure consolidation tests on two caprock samples under saturated and isothermal conditions. The general experimental procedure is described in this section. The pumps and the system stainless tubings were refilled from fluid reservoirs prior to connecting the hydrostatic cell to the system. The test samples were mounted on a pedestal of diameter of 2.5 in, located on the base unit of the hydrostatic cell; this base pedestal has one entry port to provide water flow to the sample, and the sample is sealed by several latex membranes. During testing the sample is sandwiched between two filter paper and porous stones to homogenize pore fluid flow through the sample. In this arrangement, the sample can be subjected to cell pressure as well as to flow through the sample at a known hydraulic gradient. The typical sample assembly (without latex membranes) on the base pedestal of the triaxial apparatus is shown in Figure 22. Then fill the cell with silicon oil, move the cell into the oven and connect the fluid lines to the hydrostatic cell. After the hydrostatic cell was assembled, the procedure for saturating the specimen was begun. The specimen was first saturated by increasing the pore pressure in steps while maintaining an effective stress in the specimen of about 200 kPa. After confirming the specimen is ~90% saturated from Skempton coefficient B, the temperature in the heating chamber was increased to a constant 40 °C. In general, due to the relative lower permeability of shale, several days were required for the completion of the saturation phase.

The mechanical loading-unloading cycles as well as permeability measurements were performed in steps. In each loading step, the excess pore water pressure was allowed to completely dissipate (drained conditions). The pore pressure was kept constant around 2.8 MPa during the whole consolidation experiments. During one given loading stage, after applying selected confining stress levels and waiting for several days until consolidation was finished. For 1st Sample, one loading stage was lasted approximately 4 to 7 days, for 2nd Sample was around 3 days because the large sand content accelerate the rate of consolidation. After each consolidation stage, the permeability was measured. The permeability was measured using steady-state flow method. To do this, the upstream pressure was increased 200 kPa to reach the differential pressure across the specimen and then measure the steady flow rate and apply Darcy's law to calculate the permeability.

The volumetric strain evolution over time was recorded by the pump volume while the pressure in the pump controller unit was held constant. Following the macroscale test, the textural changes in the specimen were investigated with SEM images (discussed in Chapter 3.3.5). During the experiments, the confining pressure and pore pressure were monitored by the pressure transducers, the steady state flow was calculated from the pump volume change rate.



Figure 22. Assembly view of the sample setup

3.3 Test Results and Analysis

3.3.1 Initial Saturation

During the saturation process, the degree of saturation was monitored in terms of the Skempton's B parameter. Figure 23 shows the pore pressure response to the cell pressure increase step. The slope of the line gives a Skempton's B coefficient of 0.88 for 1st Sample and 0.96 for 2nd Sample. These values are believed to correspond to a degree of saturation above 90% and the specimens were ready to initiate consolidation. Both samples were saturated around the same time

scale (around four days), but the value of B is quite different, 2nd Sample apparently was much easier to be saturated compared to the 1st Sample, because 2nd Sample has more sandy layers.



Figure 23. Pore pressure as a function of confining stress. The slope of the line gives a Skempton's B coefficient of (a) 0.88 for 1st Sample; (b) 0.96 for 2nd Sample.

3.3.2 Isotropic consolidation and modified Cam-clay model

After saturation, the specimen was allowed to isotropic consolidate in the cell under a sequence of increasing confining pressures. To ensure each consolidation stage is 90% complete, sufficient time (1st Sample: 4 to 7 days; 2nd Sample: 2 to 4 days) need to be allowed between successive increments. The concepts presented in this chapter are based on the principle of Terzaghi's effective stress. The behavior of all porous media is controlled by effective stress. Eq. 17 defines the mean effective stress in triaxial cell under hydrostatic condition:

$$p' = \frac{\sigma_1' + 2\sigma_3'}{3} = \sigma_3' \ (\sigma_1' = \sigma_3')$$
 Eq. 17

where σ'_1 and σ'_3 are the principle effective compressive stress and p' is the mean effective stress. Volumetric strain in the triaxial cell is defined as:

$$\varepsilon_{v} = \frac{\Delta V}{V_{0}} = \varepsilon_{1} + 2\varepsilon_{3} = 3\varepsilon_{1}(\varepsilon_{1} = \varepsilon_{3})$$
 Eq. 18

The new cross-sectional area is thus can also be evaluated, this new area value can be used for permeability measurement at different stress levels:

$$A = A_0 \frac{1 - \varepsilon_v}{1 - \varepsilon_1}$$
 Eq. 19

In this test, we use the fluid drained from the specimen to back pressure pump to calculate specimen volumetric change during consolidation. Then the change of void ratio is calculated as follow:

$$\Delta \mathbf{e} = \varepsilon_{\nu} (1 + e_0) \qquad \qquad \text{Eq. 20}$$

Figure 24 shows the typical results: compression curve of specimen in terms of void ratio and the logarithm of the mean effective stress. The 2nd Sample's consolidation curve consists of an unloading and recompression curve, bends into the virgin compression curve. The virgin consolidation curve represents the compression of the clay for additional loading and is approximately a straight line when plotted against the logarithm of the pressure. During isotropic consolidation, the bulk volume and the porosity decrease when increasing stresses are applied. In general, three stages of deformation can clearly be identified during loading-unloading-reloading cycles: an initial stage with a high compressibility with the applied stress smaller than preconsolidation pressure, followed by a fairly linear evolution of void ratio reduction or specific volume vs. stress (normal compression line), and finally a linear isotropic unload/reload lines (elastic swelling line). The initial compression stage is associated with the closure of existing cracks, and the unloading-reloading stage is characterized by the development of elastic deformation processes. Large volumetric strains occur at the preconsolidation pressure or isotropic yielding stress p_{c0} which is evident in the semi-logarithmic plot and can be determined as the intersection of the two linear segments of the curve as suggested by Balasubramaniam et al. (1980) and Cui et al. (1996), 3 MPa (see Figure 25). This value is assumed to be the maximum past mean effective stress. The in-situ effective stress of the shale sample p_0 was approximated from the well log analysis displayed in Figure 12, to be 1.28 MPa. Then over-consolidation ratio R could be determined following Eq. 21, which is 2.34 in this location. However it's worth to mention that the locations of both the virgin compression line and the value of the pre-consolidation pressure determined in the laboratory are influenced by many factors including 1) the effect of sample disturbance (Brumund et al. 1976); 2) the rate and increment of loading during consolidation (Graham et al. 1983; Leroueil et al., 1988). Thus estimations of the overconsolidation ratio of clay deposits in the field are dependent on the loading rates and paths used in laboratory tests for determination of the preconsolidation pressure.

$$R = \frac{p_{c0}}{p_0}$$
 Eq. 21



Figure 24. Typical result of isotropic consolidation test with unloading-reloading cycles: void ratio-effective stress relationship (a) 1st Sample; (b) 2nd Sample.



Figure 25. Graphical construction method for determination of preconsolidation pressure: (a) 1st Sample; (b) 2nd Sample.

The general shape of volumetric change of the Wabiskaw 'D' caprock against log time for a sustained total stress is shown in Figure 26, the full original curves for all of the applied loading steps are shown in Appendix A. For convenient comparison, two samples time-dependent deformation at the same stress level are put right next to each other. Both samples clearly show three distinct stages of consolidation: initial compression, primary consolidation and secondary consolidation. However, 2nd Sample's rate of secondary consolidation is much higher than the primary one. For clayey soil, the creep behavior is usually associated with the response of the soil in secondary compression. Our experimental data clearly slow that creep not only occur in Wabiskaw sample, but dominating the deformation in the long-term. Such creep behavior is valuable and worthy to analyze for wellbore integrity analysis, see more discussion in Chapter 3.3.5.



Figure 26. Time-volumetric strain plot during one consolidation stage at a) p' = 2 MPa; b) p' = 4 MPa; c) p' = 6 MPa; left side is 1^{st} Sample data, right side is 2^{nd} Sample data.

The experimental behaviour described can be interpreted in the light of modified Cam-clay model first proposed by Roscoe and Burland in 1968. This model is widely referenced and has been widely used in solving boundary value problems in geotechnical engineering practice (e.g., Gens and Potts 1988; Yu 1998; Potts and Zdravkovic 1999), although it was developed originally for reconstituted clays. Because of some familiarity with the model by the geotechnical profession and it captures well the essential behaviour of clayey soil, the modified Cam-clay model was chosen as the appropriate constitutive model for describing caprock stress-strain behavior under isotropic consolidation condition. The results of an isotropic compression test are conventionally expressed in terms of specific volume ($\nu = 1 + e$) rather than void ratio (*e*); the equation that governs normal consolidation line in the model is expressed as:

$$v = v_{\lambda} - \lambda ln \frac{p'}{p_1}$$
 Eq. 22

where λ and ν_{λ} are two material parameters, and p_1 is a reference pressure. (Note that ν_{λ} is the value of the specific volume at the reference pressure)

An unloading-reloading excursion determines the elastic swelling line. The equation of the swelling line has the form:

$$\nu = \nu_{\kappa} - \kappa ln \frac{p'}{p_1}$$
 Eq. 23

where κ is a material constant, and the value of ν_{κ} for a particular line depends on the location of the point on the normal consolidation line from which unloading was performed.

The experimentally determined values of specific volume are plotted against the logarithm of the mean effective stress in Figure 27, the four material parameters in Eq. 22 and Eq. 23 are also determined and shown. Pre-consolidation pressure is another useful parameter in characterizing the behavior of Cam-clay material, determining the initial size of the yield surface in the model.



(b) 2nd Sample

Figure 27. Normal consolidation line and unloading-reloading (swelling) line for Wabiskaw isotropic compression tests, the necessary associated material parameters are also determined: (a) 1st Sample; (b) 2nd Sample

It is often helpful to run a simple test of the selected material model before using it to solve the full-scale, boundary-value problem. This can provide insight into the expected response of the model compared to the known response of the physical material. The approach chosen to quantify the parameters of a modified Cam-clay model was to perform numerical experiments with the same testing conditions as we applied in the laboratory. The numerical experiment approach was applied to match the relationship between the specific volume and mean effective stress. Modified Cam-clay model parameters were adjusted until satisfactory matches were obtained with experimental results. The geomechanical simulator, FLAC^{3D}, was used for the numerical experiments, which are discussed below.

A single element cube geometry with size and dimension of one is to be modeled in the numerical experiments (Figure 28). The numerical experiments were conducted based on the following steps:

- 1) Set up the simplified model and boundary conditions as that used in the lab tests;
- Apply the modified Cam-clay model and related model parameters, such as slope of swelling line and normal consolidation line, etc. and
- 3) Run the numerical experiments with the same stress paths as in the laboratory (in order to minimize shocks to the numerical model, the load increment is smaller in the numerical experiment, 200 kPa/step). Adjust the model parameters and relationships to obtain a good match to the laboratory tests.



Figure 28. Calibration model in FLAC^{3D}

A total of two numerical experiments were performed to simulate the 1st Sample and 2nd Sample different behavior. The matches obtained with experimental data for typical stress paths

are shown in Figure 29. Table 3 lists the 8 parameters necessary to characterize the modified Camclay model completely and gives the appropriate values used. The maximum value of bulk modulus in $FLAC^{3D}$ is quite different from the experimental value (specified in Chapter 3.3.4, where the bulk modulus is the inverse of the bulk compressibility). Because the value of the *K* from the Cam-clay model in the Itasca code changes as a function volume and the mean effective stress:

$$K = \frac{\nu p}{\kappa}$$
 Eq. 24

The input values of K_{max} are used in the mass scaling calculation performed in FLAC^{3D} to ensure numerical stability.

Parmeters	1st Sample	2nd Sample
maximum elastic bulk modulus, K_{max}	20 GPa	2 GPa
slope of swelling line, κ	n/a	0.04
slope of normal consolidation line, λ	0.03	0.13
frictional constant, M	1.2	1.2
reference pressure, p_1	1 MPa	1 MPa
pre-consolidation pressure, p_c	3 MPa	3 MPa
specific volume at reference pressure on normal consolidation line, ν_{λ}	1.39	1.59

Table 3 Modified Cam-Clay Model parameters for Wabiskaw Samples



(b) 2nd Sample

Figure 29. Specific volume versus logarithm of mean effective stress, lab tests simulated by FLAC^{3D} and comparison with experimental results: (a) 1st Sample; (b) 2nd Sample

3.3.3 Permeability

Permeability is a fundamental property of rocks and other geomaterials that characterizes the ability for fluids to permeate through the accessible saturated pore space under a hydraulic potential gradient. Permeability of rocks is widely assumed to be a rock property, which is largely a function of the connected porosity, and the connectivity. But a study of the stress state of the sample and its effect on permeability reveals it is more a 'process' than a 'material' property, because it changes within one formation due to changed boundary conditions (e.g. stress, deformation). Changes in permeability that take place in a reservoir due a change in effective stresses caused by engineering activities related to fluid withdrawal (pressure, temperature, etc.) are of direct concern to the petroleum engineers and hydrogeologists. Similarly, Wabiskaw caprock formation can undergo large decrease in porosity, both in situ and in the laboratory. As such, the permeability of Wabiskaw member is dependent on its porosity and the effective stress conditions. The objective here is to establish effective pressure and porosity laws for the permeability of Wabiskaw 'D' caprock.

At the end of each consolidation stage the absolute sample vertical permeability was measured using the constant pressure gradient technique. This involved imposing a fixed pressure difference between the upper (downstream) and lower ends (upstream) of cylindrical specimen and measuring the resulting discharge rate from the upstream pump and into the downstream pump. The permeability is then determined from Darcy's law:

$$Q = \frac{kA}{\mu} \frac{\Delta p}{L}$$
 Eq. 25

where Q (m³/s) is the volumetric flow rate across the sample cross-section area; μ (Pa·s) is the dynamic viscosity of fluid; $\Delta p/L$ is the pore pressure gradient across the specimen; A (m²) is the area of cross section of the specimen and k (m²) is the average permeability.

The pore pressure difference is measured from the pressure transducers, and volumetric flow rate is retrieved from upstream pump. Ideally, the permeability measurement should be made at steady state; this is only achieved when the flow into Pump B is equal to the flow from Pump A. In practice, the flow associated with the reduction in pore-water volume due to ongoing consolidation, means that the downstream flux will exceed the imposed flow into the sample. This

additional flow is small (~5%) compared to the flow induced by the pressure difference which means that the steady state permeability can be estimated to a reasonable accuracy from the upstream pump flow data collected at the end of each loading increment. For isotropic compression testing condition, all principle stresses are assumed to be equal and the mean effective stress is calculated from Eq. 17. The typical time development of the pressures acting in and around the sample during the isotropic compression test is shown in Figure 30.



Figure 30. Schematic time development and schematic view of pressure and stress acting on the experimental specimens under isotropic compression testing condition (Heiland 2003)

As expected, the permeability of the two samples decreases continuously with increasing mean effective stress (Figure 31), the y-axis represents (k/k_b) , where k_b is the base permeability at effective mean stress value of around 1 MPa. The reduction in permeability (almost 60% and 70% for two samples up to 4 MPa respectively) was comparable to the published values for

Clearwater formation during isotropic compression (Zadeh 2016). The consistent reduction of the permeability with confining pressure in the loading cycle would suggest that there is micro-crack closure and gradual pore reduction. Not only is there a decrease in absolute permeability with contraction; but also the variation is almost an order of magnitude change compared to the initial value. For 2nd Sample, it was also subjected to loading-unloading-reloading cycles, the permeability results are shown in Figure 32. The unloading and reloading stage didn't cause significant permeability variation (around 10%) compared to initial loading stage. Experiments data indicate the irreversible reduction in the permeability that accompanies the application of compressive isotropic confining pressure to this sample of Wabiskaw shale. The presence of permeability hysteresis would suggest that there are irreversible changes to the pore space, in the form of irreversible pore reduction, resulting from changes to the porous fabric, during the compressive pressurization of the specimen. The permanent volumetric strain is one positive phenomena for wellbore integrity issues, indicating that the cyclic loading-unloading stages during the in-situ reservoir operation will equip the caprock with permeability decreasing properties, assisting in restraining injected steam and heated reservoir fluid. The principle of the caprock hydraulic self-healing properties will be discussed in Chapter 3.3.5.



(b) 2nd Sample

Figure 31. Permeability reduction during the isotropic consolidation test: a) 1st Sample; b) 2nd Sample



Figure 32. Variation of permeability with isotropic stress history followed by loadingunloading-reloading

If we plot the permeability vs. mean effective stress on a semi log plot, and the permeability vs. porosity evolution in log-log scale, we can see that the permeability evolution is fairly linear (see Figure 33 and Figure 34). Therefore two empirical laws proposed by David et al. (1994) can be applied here to describe the permeability evolution with effective stress and porosity in isotropic consolidation for Wabiskaw shale, corresponding respectively to a decreasing exponential law and a power law.

$$\mathbf{k} = k_0 \exp(-\gamma_1 p') \qquad \qquad \text{Eq. 26}$$

where k is the permeability at the mean effective stress p', k_0 is the permeability at zero mean effective stress, γ_1 is the pressure sensitivity coefficient, n and n_0 are porosity values corresponding to the permeabilities k and k_0 , and α_1 is the porosity sensitivity exponent. The parameters fitted for the Wabiskaw 'D' caprock specimens are listed in Table 4. The values of γ_1 has been experimental determined for natural fault rocks is 0.0045-0.014 MPa⁻¹ for clay-free fault zones and 0.012-0.055 MPa⁻¹ for clay-rich fault zones (Morrow et al. 1984; David et al. 1994). The power-law exponent α_1 ranges from 1 up to 25 for consolidated geologic materials (David et al. 1994; Bernabé et al. 2003). The values of γ_1 and α_1 for Wabiskaw sample have notably larer values, behaved similar to sandstone subjected to hydrostatic cataclastic compaction (characterized by sandstone grain crushing and pore collapse). Larger values of γ_1 correspond to a strong response to changes in effective stress. The published data from Zadeh (2016) also applies the same relationship to describe the stress dependent permeability evolution, giving a value of γ_1 equal to 0.7 for Clearwater formation. Thus both Wabiskaw formation and Clearwater formation as primary caprock in SAGD reservoir is highly sensitive to stress and porosity change and it's necessary to consider the coupling effect between geomechanical model and fluid-flow model within this formation. The developed numerical relations (Eq. 26 and Eq. 27) could be later applied for coupling caprock simulation study. Previous work suggests that the permeability reduction that occurs in samples with large values γ_1 results from crack closure (David et al. 1994). It's worth to mention here, permeability reduction under istropic or hydrostatic compression condition is the highest among various kinds of stress paths, and lower stress paths tend to cause less permeability reduction (Morita et al. 1992; Ruistuen et al. 1996).

Generally, for natural rocks under isotropic compression, there are three phases of compactional behavior can be distinguished: 1) a high reduction rate at low effective stress, which is probably due to microcrack closure; 2) the rate of permeability reduction decreases at higher effective stress, and is then followed by an approximately exponential decay; 3) when the effective stress is increased further, porous rocks exhibit a sharp decline in permeability which indicates the onset of non-elastic deformation, this is grain crushing (Heiland 2003). Beyond the critical pressure for grain crushing a further decrease of permeability takes place. In the experiments, it's hard to differentiate between the phase 1 and phase 2, thus the equation we proposed (Eq. 26 and Eq. 27) here include both of the phases. In terms of the phase 3, because the maximum effective stress applied is still relatively small, there's no evidence showing we've passed the critical pressure state. However, in some parts of the specimen, the grain crushing may have already occured, but it's hard to detect from the overall permeability measurement. The grain crushing behavior though is important to analyze in the future, because it may be one possible reason for explaining caprock creep mechanism (see more discussion in Chapter 3.3.5). For large-scale well
integrity analysis, caprock permeability evolution is important and necessary to characterize the geomechanical effects on hydraulic behavior for caprock formation. This means that for most reservoir depletion scenarios a continuously decreasing permeability trend can be assumed.

Table 4 Effective pressure sensitivity coefficient and porosity sensitivity exponent for the	1e
Wabiskaw 'D' caprock samples	

	$\gamma_1 (MPa^{-1})$	$k_0(\mu D)$	α_1	n ₀
1st Sample	0.23	0.33	15.20	0.28
2nd Sample first loading stage	0.26	2.36	6.72	0.34
2nd Sample reloading stage	0.14	0.47	14.82	0.22



(c) 2nd Sample, reloading stage





(c) 2nd Sample, reloading stage



3.3.4 Poroelastic properties

After the 1st Sample testing, thermal insulation protection around the pump was added which helped to improve the volume measurement of the pumps. With this improvement the cell pump volume change shows smooth varying information. In this way, the cell pump volume change can be used to represent sample bulk volume change (ΔV_b), the pore volume change (ΔV_p) could be obtained as the volume change of pore fluid produced from the sample during consolidation test by the back pressure syringe pump. With these two measurements, poroelastic properties are available from the experimental work. Due to the thermal insulation adjustments, only the 2nd Sample's results are analyzed.

A petroleum engineer describes fluid flow in reservoirs in terms of the permeability, porosity, and compressibility of the rock. The influence of rock compressibility on fluid has been studied extensively within the theoretical framework of poroealsticity. The poroelastic coefficients can be measured experimentally under isotropic conditions (Laurent et al. 1993). The discussion of the compressibility from our geomechanical tests will be based on the relationships presented by Zimmerman et al. (1986). For completeness, the relationships are repeated here to reflect the stress quantities measured during geomechanical tests. The terms C_{bc} and C_{pc} are used to refer to the bulk compressibility, the pore volume compressibility. These compressibilities can be defined as follow:

$$C_{bc} = -\frac{1}{V_b} \left(\frac{\partial V_b}{\partial P_c}\right)_{P_p}$$
 Eq. 28

$$C_{pc} = -\frac{1}{V_p} \left(\frac{\partial V_p}{\partial P_c} \right)_{P_p}$$
 Eq. 29

where V_b is the bulk volume, V_p is the pore volume, P_c is the confining pressure and the P_p is the pore pressure.

In the experimental facility design, we use the 1/8 inch diameter tubing and reduced the lengths as much as possible to remove dead volume. This approach helped to reduce errors induced by compressibility of the system to a minimum. The four variables P_p , P_c , V_b , and V_p , are measured or controlled independently and simultaneously. The syringe pumps coupled with pressure transducers are used to control and measure the pressure and the volume change. The results from

an isotropic compression test are typically presented here as mean effective stress versus volumetric strain (Figure 35). It can be seen the strongly non-linear response of the Wabiskaw sample under isotropic compression and the existence of the permanent strains. During the test, the bulk volume change and pore volume change of a specimen during the application of the cell pressure provides a true measure of the isotropic bulk compressibility C_{bc} and pore compressibility C_{pc} of the specimen. Figure 36 shows the distribution of C_{bc} and C_{pc} over a range of mean effective stress from 0.2 to 20 MPa. As mentioned before, this compressibility curve is obtained by varying the confining pressure on the specimen without changing any pore pressure within the specimen. These compressibilities are clearly not constants and decrease with increasing effective stress showing that the material becomes stiffer with increasing stress. This phenomenon is commonly observed in granular porous rocks, due to the closure of the pre-existing microcracks and compaction of the rock matrix. The differences between these measures of compressibility become large as the p' decreases. If the recovery process being simulated involves an increase in p' within the formation (e.g. production process during SAGD operation), the magnitude of error in choosing the wrong compressibility value may be relatively small. If, however, the recovery process results in a decrease in p' (e.g. injection process during SAGD operation), an incorrect selection of compressibility can result in significant error.



Figure 35. Isotropic compression results expressed as stress-strain curve of 2nd Sample



Figure 36. Variations in isotropic compressbilities of 2nd Sample

For densely packed geomaterial, the poroelasticity effect on the effective stress is significant. The form of the effective stress taking the poroelastic effect is given by:

$$\sigma' = \sigma - \alpha_2 P_p \qquad \qquad \text{Eq. 30}$$

where σ' is the effective stress, σ is the total stress, P_p is the pore fluid pressure, and $\alpha_2 = 1 - K_b/K_s$ is the Biot coefficient (Terzaghi, 1936 and 1943; Biot, 1941). Biot coefficient is an important parameter used to determine the influence of pore pressure on rock deformation. The value of α_2 decreases from unity as the porosity decreases, because the bulk modulus of the shale skeleton K_b approaches that of the bulk modulus of the individual solid grains K_s . In simple terms, the poroelastic effect comes from the deformation induced by the pore pressure changes on the volume of the solid portions of the rock (Katsuki and Gutierrez 2013). For some rocks, the changes in solid grain volume are comparable to the changes in the volume of the rock matrix. As effective stresses affect shear strength and deformability, these are affected as well by poroelasticity. Thus poroelastic effect can have important impact on the behavior of caprock.

When pore pressure remains constant, Biot coefficient α_2 is calculated as Eq. 31:

$$\alpha_2 = 1 - \frac{K_b}{K_s} = 1 - \frac{\Delta V_s}{\Delta V_b} = \frac{\Delta V_p}{\Delta V_b}$$
 Eq. 31

where ΔV_s is the solid grain volume change. The Biot coefficient observed during consolidation is shown in Figure 37 as a function of the mean effective stress and porosity. The value of the Biot coefficient is in the 0.4 to 0.8 range. The Biot coefficient is unlikely to be solely a function of mean effective stress or porosity, but it does depend on the specimen stress state and loading history. The general trend of Biot coefficient evolution against stress and porosity from the observation is α_2 decreases as stress increases and increases as porosity increases, which is similar to the previous research on natural rocks and shales (e.g. Laurent et al. 1993; Fabre and Gustkiewicz 1997; Katsuki et al. 2014). In fact, the Biot coefficient is related to the stiffness of the material: a lower stiffness translates into a greater Biot coefficient (Ferrari et al. 2016). Thus the consolidation test is considered able to appropriately characterize the stress dependency of Biot coefficient of the Wabiskaw shale sample.



Figure 37. Biot coefficient evolution of 2nd Sample during consolidation against (a) mean effective stress; (b) porosity

3.3.5 Discussion about Wabiskaw creep behavior and its impact on wellbore integrity analysis

The observations of time-dependent deformation of Wabiskaw samples during consolidation test results were reported above (see Figure 26, and Appendix A). In the late stage of one consolidation pressure level, Wabiskaw exhibits obvious creep behavior, in which the deformation of soil develops with time at a state of constant effective stress. It is of significance to investigate the 'creep' behavior of Wabiskaw formation for wellbore integrity analysis where the long-term deformation of the caprock formation need to be concerned in the analysis. In this part, the creep mechanism behind the behavior of Wabiskaw samples during the consolidation test are discussed. Several processes and factors influencing the behavior were given for the future deeper analysis.

Of all aspects of rock or soil behavior, creep is perhaps the most difficult to take from a theoretical concept to engineering design (Dusseault and Fordham 1993). Study of rock and soft clay creep began late in the 19th century, current researchers have already realized some engineering aspects like saltrock mine, storage cavern and nuclear repository designs must address creep as it dominates their behavior. However, there is no analysis on SAGD wellbore design within caprock formation considering anything about caprock creep behavior, because creep testing of clay shales is one of the most challenging laboratory problems in reservoir goemechanics, due to its slow pore pressure transients contribute to the time dependency of deformations. Consolidation actually is diffusive fluid transport processes driven by applied pressure, temperature, concentration and saturation gradients. All four factors may be evidenced in shales encountered in SAGD operations. During thermal recovery process, the creep of caprock depends on two external variables of stress and temperature and the internal variable of structure. The understanding of Wabiskaw shale's creep behavior happened during the consolidation test can help reflect the underlying fundamentals of Wabiskaw long-term deformation mechanism.

First phenomena need to be considered is the effect of Wabiskaw secondary compression or consolidation. The response of saturated clay in the phase of secondary compression is usually referred to as the creep behavior of saturated clay. According to the conventional consolidation theory, the consolidation could be separated into three phases. The first phase is considered a relatively instantaneous distortion, called initial compression. The second phase, named primary consolidation is controlled by the hydro-dynamic flow of water through the pores of the soil, and this process is referred to the theory of consolidation that Terzaghi built as the noteworthy foundation in 1941 (Terzaghi 1941). The third phase, creep or so called secondary compression is generally defined as the deformation under a constant effective stress, also a time-dependent deformation, which is not considered to be directly related to the dissipation of excess pore-water pressure. It usually is observed experimentally after excess pore-water pressure has virtually dissipated, but it also occurs simultaneously with the equalization of excess pore-water pressure which suggests that the creep contribution starts simultaneously with the primary consolidation. This hypothesis has been widely accepted by geotechnical community (Leroueil 1996, Kabbaj et al. 1988, Aboshi 2004, Degago et al. 2009). The reason for secondary compression is that the soil structure is susceptible to a viscous or creep deformation under the action of sustained stress as the fabric elements adjust slowly to more stable arrangements. The rate of secondary compression is controlled by the rate at which the structure can deform, as opposed to the rate of primary consolidation, which is controlled by Darcy's law, which determines how rapidly water can escape from the pores under a hydraulic gradient. During secondary compression, the soil particles continue to rearrange themselves, with a net decrease in void volume. Increased interparticle resistance to deformation results from more efficient particle packing and enhanced interlocking of rough particle surfaces (Terzaghi et al. 1996). During our Wabiskaw consolidation test, drainage is allowed, thus following the initial large load application, the pore pressure dissipated with accompanying volume change. The development of complete effective stress and void ratio equilibrium may take a long time (during the test, for 1st Sample we waited for 4 to 7 days, for 2nd Sample we waited for 2 to 3 days). Usually for 1D consolidation behavior, the creep deformation is characterized by the dimensionless coefficient of secondary compression (C_{α}). For 3D isotropic consolidation, in the Soft Soil Creep (SSC) model developed by Vermeer and Neher (1999), they introduced a parameter μ^* , called the modified creep index (see Figure 38) which can provide an indication of the significance of secondary consolidation effects for Wabiskaw shale and also help us compare the Wabiskaw creep behavior under different stress levels quantitatively. The parameter μ^* can be obtained by measuring the volumetric strain and plotting it against the logarithm of time (Figure 39), its value is independent of the period of elapsed time, sample thickness and load increment ratio for both normally consolidated clay and overconsolidated clay. The values of modified creep index fitted for Wabiskaw samples data are summarized in Table 5

and Table 6. The parameter μ^* appears in general to be stress-dependent. For 1st Sample, with increasing values as the mean effective stress increases; for the same mean effective stress the 2nd Sample reveals greater secondary consolidation effects with respect to the 1st Sample, but the value of μ^* generally decreases with the increasing stress. The observed trend reveals that creep effects must be carefully considered when working at caprock analysis. But it is worth to mention here, for both samples, the time we spent to wait for equilibrium state may not be long enough especially for the 2nd Sample at higher effective stress. Also due to the complexity of secondary compression, it's hard to separate secondary compression from consolidation deformation curve, because parts of the sample near the drainage surfaces may be fully consolidated, and therefore undergoing "secondary" compression, while portions near the center of the layer are still in "primary", both types of deformation contribute to the total deformation change that we saw from experimental data. All above factors may cause inaccurate estimate of the value of μ^* .



Figure 38. Graph of volumetric strain vs. natural logarithm of time



Figure 39. Volumetric strain vs. log time data: a) 1st Sample results; b) 2nd Sample results

		1st Sample			
Stress Level p' (MPa)	2.15	4.15	6.13	8.11	10.94
μ*	0.0006	0.0007	0.0009	0.002	0.0065

Table 5 Modified creep index for 1st Sample at different stress levels

Table 6 Modified creep index for 2nd Sample at different stress levels

2nd Sample						
Stress Level p' (MPa)	2.18	3.18	5.17	9.17	11.17	15.16
μ*	0.0056	0.0078	0.0043	0.0025	0.002	0.0014

Deformation behavior between the two tested samples are different mostly due to the clay content. Generally, 2nd Sample appeared higher rate of secondary consolidation than the 1st Sample. It is seen that the clay fraction of 1st Sample is twice as much as the 2nd Sample from XRD results shown in Table 1. Sone and Zoback (2010) found from their experiments that the clay-rich shale samples will exhibit high-ductility, while clay-poor samples exhibit low-ductility. These results suggest that clay content is the strongest control on various deformational properties amongst other parameters. To summarize, the micromechanical properties and structures of clay shale are responsible for defining much of the macroscopic behaviour evident during secondary consolidation or creep deformation. In order to better understand aspects of clay shale creep behavior, we compared SEM images of the Wabiskaw 'D' caprock before and after the mechanical loading tests. Several SEM micrographs are shown in Figure 40, taken from cuttings when preparing for the tested cylindrical sample (left hand side) and the deformed samples after the testing (right hand side).

In general, several processes at the structural and aggregate level maybe involved during the Wabiskaw shale time-dependent deformation observed from SEM photos. First is timedependent process of particle rearrangement: creep can lead to rearrangement of particles into more stable configurations. Moreover, the effect of leaching caused by a flow of distilled water need to be paid attention. The Wabiskaw samples used for this investigation were from the site which in-situ was salt water saturated. However in the experimental study, fresh distilled water was used as the pore fluid to simulate the injection of steam during the SAGD operation. The steady state permeability test is actually accelerating the flow of the fresh water through the sample. A flow of water through the sample will not in itself cause any changes in its geotechnical properties, but in this case the percolating water is distilled water, whereas the water confined in the voids of the Wabiskaw is salt water. The flow therefore results in a gradual exchange of the salt water in the voids with fresh water and such leaching of a marine formation has an almost dramatic effect on the properties of the clay shale, especially for 1st Sample who has larger content of swelling clay minerology. Desalination will cause the swelling clay to be more mobile than before, involving a series of structural readjustments. That is mainly the reason for 1st Sample in the late higher stress condition, the rate of creep consolidation suddenly increase a lot compared to the low effective stress. From the Figure 40, we could clearly see after the consolidation tests, the clay aggregates have migrated into irregular-shaped voids and are more tightly stick associated with the sand grains. The reason for 2nd Sample behaves more creep deformation than 1st Sample is more related to the water content. Time-dependent deformations are more important at high water contents than at low. Water may "lubricate" the particles and possibly increase the creep rate.

The second process that might happen is particle breakage during creep: the amount of particle breakage increased with load duration (Leung et al. 1996). This process is more obvious for sandy soil. It is widely accepted that the compression of sand at high stress levels is accompanied with grain crushing. However, from the permeability evidence (see in Chapter 3.3.3), we haven't reached the sample overall grain crushing critical pressure, but it may be possible some parts in the dense sand layers have reached the critical pressure. The amount of crushed grains depends on grain size distribution, grain shape, packing density, grain hardness and the presence or absence of water (Hardin 1985). Microscopic observations have revealed that angular protrusions of the grains were ground off, producing fines, the fines and clay minerals fill the voids between larger particles and crushed particles may progressively rearranged themselves with time. This process could help explain the 2nd Sample, as the effective stress is increasing, the rate of deformation is getting smaller because the void spaces were dramatically decrease, then it's getting harder for the small particles movement. More specifically, the coefficient of secondary compression decreases with decreasing void ratio, this is pretty common in sandy soils like Ottawa sand (Wang 2010).

The third possible process is time-dependent strengthening of soil structure. As mentioned above, for 2nd Sample, the creep rate is decreasing, along the process the structural changes that may occur and cause an increase in soil stiffness when the soil is subjected to further stress increase (Figure 36, the bulk and pore compressibility is decreasing as effective stress increases). Time-dependent changes of this type are a consequence of "aging" effects, which alter the structural state of the soil. In fact, consolidation is the time-dependent decrease in volume of soils subjected to loading and leads to increase of shear strength as well as stiffness of clay. Mechanical and chemical processes are possible causes of aging. In terms of the mechanical process, aging is due to a continued rearrangement of particles resulting in the increased macrointerlocking of particles and the increased microinterlocking of surface roughness. Sand has been found to increase in strength and stiffness under isotropic stress conditions in the literature (Daramola 1980; Human 1992). These increases develop even under isotropic confinement because angular particles can lock together in an anisotropic fabric. However, chemical aging is hard to come to a conclusion and has many disputes in the geotechnical history (Denisov and Reltov 1961; Human, 1992; Joshi et al. 1995; Losert et al. 2000; Mesri and Members 1990).

Fourth is time-dependent changes in soil fabric, due to the constant confining stress, we could clearly see that the voids become smaller, and particles group or cluster with time. The movement of particles lead to interlocking zones of greater local density. The interlocked state may be regarded as the final state of any one particle under a particular applied load, due to kinematic restraint. The result, with time, is a stiffer, more efficient, load-bearing structure, with areas of tightly packed particles. The increase in stiffness is achieved by shear connections obtained by the clustering. Then, when load is applied, the increased stiffness and strength of the granular structure provides greater resistance to the load and the observed aging effect is seen (Mitchell and Soga, 2005). Fifth is the time-dependent changes in physicochemical interaction of clay and pore fluid. Nakagawa et al. (1995) showed that the pore fluid composition and ion mobility changed with time, the electrical conductivity increased during the early stages of consolidation, but then decreased continuously thereafter. The electrical conductivity is dominated by flow through the electrolyte solution in the pores. During the initial compression, a breakdown of structure releases ions into the pore water, increasing the electrical conductivity. With time, the conductivity decreased, suggesting that the released ions are accumulating near particle surfaces. Some of these released ions are expelled from the specimen as consolidation progressed. A slow

equilibrium under a new state of effective stress is hypothesized to develop that involves both small particle rearrangements, associated with decrease in void ratio during secondary compression, and development of increased contact strength as a result of desalination processes. Primary consolidation can be considered a result of drainage of pore water fluid from the macropores, whereas secondary compression is related to the delayed deformation of micropores in the clay aggregates (Berry and Poskitt, 1972; Matsuo and Kamon, 1977; Sills, 1995). The mobility of water in the micropores is restricted due to small pore size and physicochemical interactions close to the clay particle surfaces.



(a) 1st Sample, large scale overview of sample



(b) 1st Sample, sand grains area



(c) 2nd Sample, large scale overview of sample



(d) 2nd Sample, sand grains area

Figure 40. Micrographs of intact Wabiskaw (left side) and deformed Wabiskaw samples after macroscale tests (right side): (a) (c) 1st Sample and 2nd Sample on a large scale overview, showing distinct boundary between high clay section and high sandy section; (b) (d) 1st Sample and 2nd Sample on a smaller scale showing part of the sand grains area.

In summary, the macroscopic behavior is controlled by the microscopic mechanisms. Soil deformation induces grain compression in sandy soil or particle bending in clayey soil, and thus causes the rearrangement of grains or particles at low stress level. At high stress level, the creep behavior of soil is induced by grain crushing or particle breakage. After that, the rearrangement of grains or particles occurs in soil. Creep deformation also depends on the effective stress path followed and any changes in stress with time. Furthermore, time-dependent volumetric response is governed by the rate of consolidation, which is a complex function of material properties, especially the permeability and compressibility. In other words, the interrelationship between stress, strain, and time for Wabiskaw shale is not simple and cannot be treated mathematically with present tests. Thus more comprehensive experimental program is necessary to reach a fully coupled analysis of Wabiskaw soil-pore fluid interaction with an appropriate time-dependent constitutive model to reconcile the long-term deformations for the sake of long-term SAGD wellbore integrity analysis.

For decades of SAGD operations in Alberta, people focus primarily on caprock failure mechanism ranging from shear failure to tensile failure, thus many of triaxial tests have been done in geomechanical lab to estimate the failure strength of the caprock. Recently coupled reservoirgeomechanical comprehensive modeling is required by Alberta Energy Regulator to predict the pressure at which failure of the caprock may occur and how the results support the proposed operating pressure. During the SAGD operation process at high pressures, many fractures and cracks could be generated due to accumulated stress and strain by the high temperature steam injection. The fractures in the caprock would lead to failures, causing serious damage to the project, such as the 2006 Joslyn steam release incident.

However, based on observation on caprock through isotropic consolidation test, we believe that the caprock has self-healing properties of natural and induced fractures which could play a major role in reducing the long-term SAGD impacts on caprock formation. Self-healing may be defined as a spontaneously-occurring process, with many possible contributory underlying mechanisms, that leads to a reduction in the impact of a fracture on both the hydraulic and the mechanical properties of the rock mass (Horseman 2001). The general principle and underlying concept of caprock self-healing property could be explained by Figure 41: if a damage is inflicted on the caprock, a crack can occur or the micro-crack within the caprock may be enlarged to macrocrack. Self-healing can take place on the microscopic to macroscopic level.



Figure 41. Common basic principle of self-healing materials: a) the mechanical load induces a crack; b) detailed view of the crack; c) a "mobile phase" is induced; d) closure of the crack by the "mobile phase"; e) immobilisation after healing. (Hager et al. 2010)

Usually, the initial compression and primary consolidation phase within caprock induced by the external stress change happened relatively faster compared to the subsequent secondary consolidation phase. Because of the caprock creep characteristics, the hydraulic crack or the "damage" can be removed due to the directed mass transport towards the crack and there would be the subsequent local mending reaction. After the healing of the crack, the previously mobile material is immobilised again, resulting in the best case in fully restored hydraulic sealing properties of caprock which could help restrain the development of steam chamber. These conceptual ideas could be easily proved by measuring the permeability magnitude, the results are explained previously (Figure 32), during the unloading stage, caprock cannot reach a restoration of the initial permeability and the reloading stage will continuously decrease the permeability.

In fact, self-healing is a complex process, which can involve hydraulic, mechanical, chemical and mineralogical changes occurring within the system. Minerals can be transported into the fracture in solution and precipitated as cementing agents. The composition and fabric of the fracture can also be altered. Given a long enough period of time and a suitable geochemical environment, it is possible for self-healing to occur in caprock. Many of the underlying mechanisms of self-healing are, again, time dependent related more to the creep mechanism. Overall itis reasonable to apply the self-healing capabilities of caprock into the real wellbore design and geomechanical modeling process. Such behaviour on the microscale could be explained by irrecoverable and plastic deformation during specimen consolidation. Based on microscale analysis, we could conclude that the "mobile phase" causing the crack closure that we mentioned above is continuously clay minerals migration under constant stress, thus micro-hydraulic cracks could be self-healed after long term.

The easiest way to include caprock creep behavior into the wellbore design is considering about the inverse phenomenon of creep, usually termed stress relaxation, which is a drop in stress over time after a soil is subjected to a particular constant strain level. From the perspective of SAGD wellbore integrity analysis, the element of caprock deformation surrounded the wellbore are restricted and caprock creep may cause a reduction of the load required to maintain that displacement. For this situation, stress redistribution again takes place as the load relaxes, the typical relaxation curve is similar to Figure 42. Creep and relaxation are two consequences of the same phenomenon, that is, time-dependent changes in structures. The problem is to determine the subsequent relaxation of the stress due to creep. To solve this, it is recommended that relaxation tests be conducted in the future (Figure 43). This kind of test requires a stiff test frame and knowledge of the constitutive law form for a material in order to interpret results. Under the traixial stress condition, an "instantaneous" length change could be imposed on the sample, then measuring the axial stress decay with time, along with lateral strain. These tests can also be performed by carrying out a constant axial stress creep test for some time, then closing the pressure control system and monitoring stresses and rates until stability ensues.



Figure 42. Typical relaxation curve (Boyle & Spence 1983)



Figure 43. Common creep test mode: relaxation tests (Dusseault & Fordham 1993)

In conclusion, creep not only influences the proneness of caprock to fracturing during thermal recovery process, but also affects the long-term reservoir response during depletion. Over geologic time scales, creep deformation could also alter the state of stress as commonly observed around salt domes. In current study, to fill an important gap in the knowledge of caprock creep phenomena, the creep consolidation behavior of the intact, overconsolidated Wabiskaw 'D' caprock has been investigated by means of drained isotropic consolidation test, the microscale analysis was done by taking SEM microphotos. Considering no study until now have established Wabiskaw shale constitutive law describing time dependent creep deformation, further studies with emphasis on the triaxial creep testing are needed for a comprehensive understanding.

3.4 Summary

Conventional reservoir simulations do not account for caprook geomechanical effect explicitly, but implicitly include the fixed properties from the in situ testing results. This chapter reports an experimental investigation of the hydro-mechanical behavior of Wabisakw 'D' caprook subjected to isotropic loading programmes. Both macroscopic experiments and microscopic analyses of deformation reveal that the hydro-mechanical properties of Wabiskaw shale is strongly controlled by the applied stress field and its microstructures. Details of the materials used, test setups, programs and results are presented in this chapter. Analyses of the test results and their incorporation in reservoir numerical simulators should be made in terms of effective stress. The essential features of the observed behavior are summarized as follows:

- Laboratory results show that the hydraulic, mechanical and poroelastic properties of Wabiskaw shale are varied with stress state. Results on two Wabiskaw shale specimens from different depths reflected significant heterogeneity in this formation.
- The modifed Cam-clay model has been calibrated based on the experimental results so that the isotropic variation of the mechanical properties of Wabiskaw 'D' caprock can be described. It is shown that this model's theoretical predictions are compared with experimental data, showing an acceptable agreement over the full range of confining pressures considered. This constitutive model and associated parameters could be used to represent caprock constitutive behavior in geomechanical model.
- Under isotropic consolidation stress path, it was found that the permeability decreases/increases continuously with mean effective stress increases/decreases, matching with the observed pore space and volume change behavior. The evolution of permeability as a function of stress and porosity could be described with empirical mathematical relationships proposed in the literature. These two mathematical expressions could be used for emphasizing the geomechanical effect on hydraulic behavior of caprock formation in coupled hydro-mechanical simulation for wellbore integrity analysis.
- Measurements from the isotropic consolidation test could also be used to characterize the Wabiskaw sample compressibility coefficients and Biot coefficient. Similar to many rocks and shales, Wabiskaw's poro-elastic parameters are not constants but vary with effective stress or porosity. Changes in compressibilities are greatest under low effective stress

conditions similar to steam injection processes. Biot coefficient increases with porosity and decreases with stress. Thus great attention must be paid to choose the right poromechanical value in analyzing the caprock deformation.

• Significant creep behavior was observed during Wabiskaw sample consolidation test. The values of the secondary compression coefficient were also reported, allowing information to be gathered on the creep behavior of the tested shale as a function of the mean effective stress. The creep behavior varies strongly with material composition like clay content. However, the mechanism behind the creep behavior is so far unclear and needs to be understood in order to constrain the constitutive creep model. Further studies with emphasis on the triaxial creep testing are needed for a comprehensive understanding, because creep deformation not only influences the long-term caprock response, but also alter the state of stress around the wellbore. The creep characteristics should be included in the SAGD wellbore design in the future by addressing the hydraulic self-healing properties of caprock or introducing a load reduction factor.

4 Axial Flow Hydraulic Pulse Testing of Well Cement

Abstract: The development of permeability in cement placed in the casing-hole annulus in oil and gas wells is a frequently encountered problem that can lead to various adverse economic and, possible catastrophic consequences. Although rarely discussed, it is critical that cement must have, and maintain, an impermeable matrix in order to provide zonal isolation. In this chapter, we investigate the hydraulic pulse test technique, which is usually considered as efficient for measuring the permeability of low permeability porous media. The efficiency of the technique is demonstrated through some pulse tests performed on one cylindrical class G oil well cement specimen. For comparison purposes, permeability was also measured using steady state methods. The results indicate that the permeability values from the pulse test were in good agreement with those obtained from the steady state test while also providing excellent reproducibility. The relevant experimental parameters for transient pressure pulse testing are identified based on the general, 1-D analytical solution proposed by Hsieh et al. (1981). In addition, the expression proposed by Brace et al. (1968) to compute the low permeability of a rock specimen from a hydraulic pulse test was examined and compared with the value obtained from analytical solution. The approach of Brace et al. (1968) tends to give lower value of permeability.

4.1 Introduction

Water plays a very important role in well cement durability. In a more general way, water is necessary for most, if not all, chemical degradation reactions to occur. These reactions can have a significant impact on the transport properties of the cement matrix. An assessment of the durability of cement wellbore requires an accurate description of potential for water transport throughout the wellbore service life, and permeability data is the most important input for wellbore durability analysis.

Hardened cement paste is a complex and evolving porous material. It results from chemical reactions, referred to as hydration, between cement clinker and water. The physical properties of the hardened cement paste depend on clinker composition, water-to-cement ratio, cement age and curing conditions (Ghabezloo 2010). The permeability of hardened cement paste is related to the pore size distribution, pore conductance, pore tortuosity and isotropy of the sample (Hughes 1985).

The importance of cement permeability is evident in SAGD wells which must maintain a permeability that is sufficiently low to prevent leakage of injected fluids out of reservoir fluid into wells. The need for the accurate estimation of the permeability characteristic of well cement is dictated by consideration of the durability of SAGD wells. Apart from determining the relative ease with which the cement can become saturated and vulnerable to reservoir fluid damage, permeability also determines the extent to which corrosive agents can attack the steel casing. The importance of permeability has re-emphasized the suggestion that in the specification of well cement durability, the governing criteria should be based on considerations of the permeability of cement and the diffusion of chemicals through cement rather than on the traditional criteria which are solely based on considerations of mechanical properties (Dhir and Byars 1993).

Permeability can be defined as that property of a porous material which governs the rate at which fluid moves through the pore structure under an imposed pressure gradient. The terms absolute permeability and intrinsic permeability are often used (interchangeably) to describe the permeability, k, of a porous medium. It is independent of the properties of the fluid migrating through the pore structure. The units associated with permeability are length squared (e.g. m²). The hydraulic conductivity, K, is a related, fluid specific property with units of length per unit time (e.g. m/s or ft/s). Throughout this chapter the term permeability will be used to signify the absolute permeability.

There are no Canadian Standards Association (CSA) or American Society for Testing and Materials (ASTM) standardized tests for measuring cement permeability. In weakly permeable porous media, classic measuring techniques based on a stable flow rates require long periods of testing to assess permeability for materials with permeabilities whose values range between 10^{-22} and 10^{-19} m². The objective of the research presented in this chapter was to develop a fast and efficient means of measuring the hydraulic conductivity of a cement sample with water, and to determine the applicability of the transient pulse technique for wellbore cements. For comparison purposes, permeability was also measured using steady-state method. This paper discusses the experimental procedures that have been developed for conducting both steady state and transient hydraulic pulse tests on one small intact oil well cement specimen. The sample was prepared with a w/c ratio of 0.6 and cured in cylindrical metal mode for 28 days. The dimensions of the sample of cement used in the experiment were ~1120 mm² in plan area and ~21 mm in height. The

circumference of the specimen is sealed to ensure the development of plane fluid flow through the cement sample along the vertical axis. Practical aspects of the experimental calibration and the procedures to facilitate the measurements of the low permeability in cement are the focus of the study. The parameter identification involved in the pulse test is based on the analytical solution proposed by Hsieh et al. (1981). A finite-element approach of the one-dimensional flow problem is conducted to simulate the pulse decay behavior, which could represent the large-scale in-situ well cement time-scale pore pressure response after the pore pressure buildup induced by the SAGD thermal energy. The simple expression proposed by Brace et al. (1968) to compute the permeability from the pulse test by disregarding the specific storage of the specimen is also examined and compared with the analytical solution. Darcy's law is utilized to calculate the permeability from steady state flow measurements.

4.2 Experimental methodology

In this study, we use both steady state flow method and transient pulse test method to estimate the permeability of well cement. The test procedure is restricted to axial flow from the cylindrical sample bottom to the top. For the pulse test, a nearly instantaneous pressure pulse is applied to the sample bottom. The resulting pressure gradient causes axial flow and the pressure decays across the entire specimen with time. The time dependence of the decay is a function of the permeability, and specific storage of the test specimen.

4.2.1 Sample preparation and curing

Non-thermal class G oil well cement was used throughout the study. The basic ingredients of cement slurry are cement, fresh water and additives. As described by API standards, cement slurry requires 44% water by weight of cement which is known as Neat Cement. However, to simulate "bad" in-situ cements, with properties like a high permeable slurry, we chose to use a water to cement ratio w/c = 0.6 in current study. Also, the anticipated permeability this sample was high enough that steady state permeability measurements could be obtained within a reasonable time yet sufficiently low that it was also within the functional range of the apparatus as designed for pressure pulse testing. The cement slurries were prepared following API Specification 10A. The mixing device for preparation of well cement slurries was a standard one liter, bottom-drive, blade type mixer (see Figure 44). The prepared slurry was poured into a cylindrical mold at

room temperature and a separation plate was put on top of each mold chamber (as shown in Figure 45). The cement samples are cast in pairs of metal molds. The length of the resulting cement cylinder is 100 mm and the diameter is 38 mm. The cement was cured in the mold in a water bath for a period of 1 day. After that, the set cement will be taken out of the mold and left to cure in water bath for a further 28 days. After this period the sample can be considered as water saturated. Prior to the permeability test, the sample was trimmed down to around 2.5 cm high. The prepared sample used in this study is shown in Figure 46. The relatively small size of the test specimen usually ensures that the test material is in an intact state. Additionally, it is expected that the chemical and pore structure of the material will not vary significantly throughout the test specimen. The final dimension of the tested specimen is 21 mm in length and 38 mm in diameter.

The geomechanical properties of the cement were estimated from data previously collected within our research group. The parameters used in the interpretation were: drained Young's modulus E = 3 GPa, drained Poisson's ratio $v_1 = 0.2$, and the porosity n = 0.375. This porosity value was estimated based on initial water cement ratio also assuming there the pore spaces are all filled with water. The bulk compressibility of the cement specimen was calculated using the relationship:

$$C_{eff} = \frac{3(1-2\nu_1)}{E} = 6.00 \times 10^{-7} \text{ kPa}^{-1}$$
 Eq. 32

The water properties used in the study are obtained from White (1986): dynamic viscosity of water at 40 °C $\mu_w = 6.53 \times 10^{-4}$ Pa·s, density of water $\rho_w = 992$ kg/m³, and compressibility of water $C_w = 4.38 \times 10^{-10}$ Pa⁻¹. An initial estimate of the specific storage of the sample may be made by assuming composed of a solid material that is incompressible could be defined by neglecting the grain compressibility in Eq. 4, turns out to be 7.44E-6 m⁻¹. This initial guess gives the order of magnitude of cement sample's specific storage.



Figure 44. Photograph of well cement mixer in GeoREF research lab



Figure 45. Photograph of a pair of metal cylinder used for cement casting



Figure 46. Photograph of cylindrical cement sample

4.2.2 The Experimental Set Up

Our newly developed, transient pulse permeability test apparatus is shown schematically in Figure 47. It consists primarily of a: heating chamber, hydrostatic cell, fluid pressure supply pumps (three syringe pumps with controlling units that generate the pore pressure and the confining pressure), data acquisition system (flow rate and pressure monitoring and recording system). The system is plumbed with stainless-steel tubing and fittings as well as high pressure Swagelok and Parker valves. For convenience, two of these valves (the by-pass valve and upstream delivery valve) are pneumatically actuated; these are controlled from outside the oven. The system also includes a CO2 gas bottle for the purpose of purging the system with gas and its desired pressure for a test is regulated by a pressure regulator. A vacuum pump is installed to facilitate the purge of the CO2 and air trapped in the system and sample during preparation.

Moreover, as mentioned above the volume of reservoir need to be optimized; to make the system more adaptable, small diameter tubing was plumbed from the pump outlet to the end of the

sample. Generally, the idea is to place the thin cylindrical cement sample in the hydrostatic cell and connect the upper and lower faces of the cement sample to the two fluid reservoirs, an upstream reservoir and a downstream reservoir. A photograph of the whole testing system is shown in Figure 48.

The hydrostatic cell can support hydrostatic pressures up to 44.8 MPa. It was designed to accommodate a right cylindrical specimen with a diameter of 1.5 in and a length from 1 to 3 in by using appropriate end caps and tubing. For pulse tests, the shorter the sample is, the faster the pulse decay will be. A one-inch sample height was determined to be appropriate for these pulse tests. The specimen is placed between two porous stones and filter paper to evenly distribute pore fluid and transmit pressure over the entire end surface. Latex membranes are set on the circumference of the sample to isolate it from the confining fluid and prevent it from radial flow at its lateral boundary. The fluid flow necessary for performing either steady state tests or hydraulic pulse tests was provided by precision ISCO 260D series pumps which are capable of supplying flow rates ranging from 0.001 ml/min to 100 ml/min. The syringe pumps are capable of maintaining or changing pressures in a controlled manner to the desired pressure values between 0.1 to 51 MPa. The syringe pump labelled B in Figure 47 is used both as the upstream pore pressure generator. The downstream pressure application is assigned to syringe pump A (Figure 47), and syringe pump C is used for the application of confining pressure. The system consists of a commercially available Data Acquisition/Data Logger Switch Unit (Agilent 34972A), which is incorporated with PC installed with a desktop LabVIEW software control panel developed by our research group. The software package is programmed to control the pumps and record the fluid pressures and associated injection/ejection volumes. In our closed reservoir configuration, a temperature of 1 °C would lead to a pressure increment of 0.5 MPa. In order to eliminate the effect of room temperature variations on the pulse decay behavior, the cell and most of the ancillary plumbing are insulated in one heating chamber. Diurnal temperature variations of 3 to 5 °C are commonly noted in the lab; however, these are reduced to 0.3 °C within the insulated chamber. The temperature of the system was monitored by four J-type thermocouples. One of these is suspended in the oven, and the other three are plumbed directly into the fluid lines through the specialized fittings. Three highaccuracy Honeywell pressure transducers were connected to the fittings close to the sample faces to continuously monitor and record pressure-time responses during the experiments. The data acquisition rate can be varied throughout each experiment; for these pressure decay measurements

the data collection rate was one the order of seconds. The transducer is rated for an operating pressure of 25 MPa and has an error of 0.25% of full scale due to nonlinearity and hysteresis. A DC power supply is used to provide the excitation voltage to the pressure transducer. Real time graphs of the pressure decay versus time can be generated during the experiments. Two comparable fixed volumes of small diameter Swagelok tubing serve as the upstream and downstream water reservoirs. These are bounded by the delivery valve and the end of the sample. The volumes of up-/downstream reservoir are approximated 10 ml and 12 ml respectively including those in the lines, the valves, the fittings and the pressure transducers. The procedures measuring the dead volume is described in Chapter 4.2.3.1. The total volume of the sample is 24 ml. Each pressure line has an atmosphere vent which can be used for de-airing the lines during the filling of the lines with water.

Two types of experiments were performed after the system (and sample) were saturated with water first, a pulse decay test to estimate both k (in m²) and S_s (in m⁻¹); then a steady state experiment to measure directly the permeability k.



LEGEND			
Φ	Valve		
	Auto Opening Valve		
Trans	Pressure Transducer		
Thermo	Thermocouple		
ATM	Atmospheric Pressure		
PRV	Pressure Relief Valve		
	Vu1		
	Vu2		
	Vd1		
	Vd2		

Figure 47. Hydraulic pulse test system design



(a) outside the oven



(b) inside the oven



4.2.3 Experimental Procedure and Results

After the construction of the system, we pressurized the experimental system with helium for a leak test using helium sniffer leak detector, to ensure every Swagelok fitting and connection a leak-free seal and won't cause pressure change more than 10 kPa over the 30 minutes, the typical time for a pulse experiment.

4.2.3.1 Dead volume measurement and reservoir compressive storage

In our test, the reservoir volumes can be limited to only include the lines from the delivery valve to the end of the sample by closing the delivery valves prior to the test. This configuration is used to optimize the time needed for the pressure decaying back to the equilibrium for low permeability measurements. However, for relatively high permeability measurement larger volumes can be used by keeping the delivery valves open.

A description of the approach taken to determine the volumes and compressive storage of these reservoirs follows. The reservoir volume and compressive storage associated with the volume from the pump outlet to the delivery valve will be noted "1", i.e. V_{u1} and S_{u1} and from the delivery valve to the end of the sample, these parameters will be associated with "2", i.e. V_{u2} and S_{u2} . The schematic explanation could be seen from Figure 47. Overall, these latter values are used to interpret the pulse test results in our system arrangement.

The volumes of the reservoirs were determined using a technique based on the PVT properties of nitrogen similar to the method that described in Zhang et al. (2013). At constant temperature, adding an accurately known volume of nitrogen to the system will cause pressure increase. However, since the product of the initial volume and pressure is equal to the product of the volume and pressure after a change in one of them under constant temperature. By adjusting the volume of nitrogen in the system with the syringe pumps, a constant relationship between the gas pressure and gas volume could be obtained; reservoir dead volumes as the constant system parameters could be derived from this relationship as well. The measured reservoir volumes include dead volumes of the pressure transducers, connecting tubes and dead volume of relevant valves. The calibration procedure which was followed in measuring the system dead volume is summarized for completeness. To increase the measurement accuracy, the upper and down

reservoir volumes were measured separately by using an impermeable dummy aluminum cylinder as the test sample. The procedure is as follows:

- Mount the sample in the hydrostatic cell (Figure 49, the surface was sealed with three latex membranes) and fasten all the internal fittings and fill the cell with silicon oil. Then transfer the cell into the heating chamber and connect the cell to the oil line of the system. Use syringe pump C to maintain the confining pressure to ensure the insulation of the sample.
- Fill the upstream and downstream lines with nitrogen and turn on the oven to keep the temperature constant at 40 °C. Adjust the gas pore pressure with the pump B. Wait until the pressure and temperature to be stable.
- 3) Close the by-pass valve to separate the upstream and downstream reservoirs from each other while keeping the upstream and downstream delivery valve open.
- 4) Slowly increase the gas pressure step by step with the pump B or A and record the pump volume at the same time. Plot the inverse of the pressure vs. volume of the syringe pump. The dead volume of lines is obtained from the intersection of the y-axis. The calculation could be explained by Eq. 33. The dead volume from the outlet of the pump to the end of the sample were measured three times separately.

$$P(t)V(t) = K = nRT$$

$$P(t)(V_{syringe}(t) + V_{line}) = K$$

$$V_{syring}(t) = \frac{K}{P(t)} - V_{line}$$
Eq. 33

5) Close the upstream and downstream delivery valve, repeat step 4) to measure the dead volume of V_{u1} and V_{d1} (dead volume from the outlet of the pump to the delivery valve) three times.


Figure 49. The assembly of the cement sample mounted in the triaxial cell

To measure S_u and S_d , the aluminum sample was used with the following procedures and conditions similar to the method used by Olsen et al. (1988, see also Chapter 2.3.2):

- Mount the dummy sample in the hydrostatic cell and fill the cell with silicon oil. Transfer the cell into the heating chamber and connect the cell to the oil line of the system. Increase the confining pressure slowly up to around 2 MPa with syringe pump C and maintain it constant.
- 2) Connect the cell pore pressure ports to the system pore pressure lines. Inject CO2 from the gas bottle to the system, while ensuring that the pressure does not reach higher than 2 MPa. With the help of the venting valve of the lines, purge the lines with CO2 several times, to make sure the lines are filled with low-pressure CO2.
- 3) Close the CO2 injection valve, open the valve connected to vacuum pump. Let the pump evacuate the tubing and reservoir system for 15 mins to remove most of the CO2 and the minor amounts of air remaining after the repeated CO2 purges.

- Refill the syringe pumps A and B with distilled water stored in the elevated reservoir at a very slow flow rate (i.e. 10 ml/min).
- 5) Use the syringe pump B to inject water into the system. This ensures the system contains only water. Adjust the pump volumes on the downstream and upstream sides to the appropriate size, by pumping the water back into the elevated water reservoir.
- 6) Slowly increase the confining pressure and pore pressure to 11 MPa and 4 MPa respectively and maintain it constant by means of syringe pump C and A.
- 7) Monitor the flow rate of syringe pump B until it remains stable, turn on the oven and keep it constant at 40 °C. Wait until the pressure and temperature to be stable, at least 1 day is needed.
- 8) Close the by-pass valve, use one syringe pump (A or B) to inject the water into the permeability system at a constant flow rate 0.001 ml/min and monitor the consequent pressure variation with either the pressure transducer or the built-in pressure transducer in the syringe pump cylinder.

The compressibility determinations are carried for pore pressures out in the range of 4 to 5 MPa. Comparable pore pressure values (and variation) should be used in the pulse test, otherwise the calibration measurement may not represent the real situation should the compressive storage of the reservoir be pressure dependent. While the pressure dependence of the compressive storage term is not expected to be significant, any trapped gas in the system was vacuumed and water was degassed, but we still choose the relatively high pore pressure to reduce the effect of pressure on the reservoir storage variation, for these testing conditions, the compressive storage of the reservoirs is practically constant. To measure the value of S_{u1} and S_{d1} , the upstream or the downstream delivery valve should be closed prior to the fluid injection. Following the storage testing, the pressure was decreased back to 4 MPa by the pump's constant pressure mode, and the resulting pump volume was compared to the initial pump volume to assess any possible fluid loss due to leakage. The volume variation was very small and therefore the reservoir compressive storage estimated was not affected by any leakage issues.

4.2.3.2 Axial flow hydraulic pulse testing

Although the principal of the pulse test is simple, certain factors are necessary to consider in the experimental system design in order to obtain accurate results. Suggestions from the literature which must be considered before our experimental system design and execution are:

- the test arrangement should be free of any leakages from interfaces and fittings either of which would contribute to an erroneous interpretation of the permeability (Selvadurai and Najari 2016).
- the removal of air entrapped within the system and sample is extremely important in implementing the successful pulse test and accurate interpretation (Selvadurai and Najari 2015) since the presence of air has direct influence on both the measured permeability and sample storage.
- 3) the temperature must be regulated carefully since thermal expansion of any component in the experimental system will affect the transient pressure measurements (Morrow and Lockner 1997). Any changes in temperature can induce excess pore pressure in the rock due to the differential thermal expansion of the pore water and the porous skeleton and affect the flow rate through the rock.
- 4) the up and downstream reservoirs must be compatible with the permeability and storage of the rock sample (Escoffier et al. 2005). The sample specific storage influences the pore pressure response shape whereas the permeability influences the time responses. Therefore, the optimal design regarding both parameters is achieved when the storage in the sample is comparable to the storage in the upper reservoir (Wang and Hart 1993). For a specific permeability testing system upper and lower limits for the permeability which can be sensibly measured exist. Outside these limits the determination of both hydraulic parameters is not accurate. When the permeability of the sample is greater than the upper limit the rate of the pressure decay will be too fast too measure accurately. When it is below the lower limit, the pressure decay in the upstream reservoir and the pressure built up in the downstream reservoir are so slow that the response may be disturbed by external effects such as temperature or internal effects such as chemical and/or mechanical transformations inside certain sample. Therefore, for each permeability test conducted on a certain rock

sample the reservoir compressibility and the sample size must be adapted to obtain a pressure response in a reasonable amount of time (from few hours until one day).

The realization of one-dimensional flow through the saturated specimen depends on two factors: the hydraulic homogeneity of the sample and the sealing action around the sample surface; the former is achieved by proper cement specimen preparation and the high confining pressure on the latex membranes is anticipated to deal with the latter.

Since it is key to ensure that there is a minimal amount of air in the sample and the experimental apparatus, the pore pressure lines of the system including the cell pore pressure tubing must be fully carefully saturated with water prior to performing either a steady state or transient hydraulic pulse test. The pulse test implementation and system saturation procedures are similar to those described in Chapter 4.2.3.1, except that the aluminum sample was replaced by the cement sample. During the sample installation inside the pressurized cell the sample may be partially de-saturated. For this reason, a 3-day saturation phase is performed inside the cell to ensure pressure homogenization throughout the sample. During this period, the sample is maintained under a confining pressure equal to 11 MPa and a back fluid pressure equal to 4 MPa while the volume of the fluid injected in the sample is monitored. This ensured that regions of the sample close to the surfaces being pressurized during permeability testing are in a near-saturated condition. After the saturation stage, the pulse tests were performed with the following procedures:

- Close all the pore pressure delivery valves and by-pass valves; stop syringe pump B (the one used to apply the back pressure during saturation).
- 2) Control the upstream pump (syringe pump B) to increase the pressure from 4 MPa to the desired value but less than 5 MPa, then stop the pump; the pressures within the specimen and in the upstream and downstream reservoirs are allowed to equilibrate for more than 15 min.
- 3) Quickly open and close the upstream delivery valve to apply a pressure pulse (a pressure rise of the order of 350 to 850 kPa) to the upstream side of the sample. This pressure pulse will drive water flow from the upstream reservoir across the sample and into the downstream reservoir. The pressure at upstream reservoir will then decrease, while in the downstream pressure will build up until new pressure equilibrium reached. The pulse decay is recorded every 1 second. The rate of pressure change in the latter portion of the decay is

very low, and this signal can be overwhelmed by background noise. For this reason, the tests were usually terminated around 1500 seconds.

- 4) Before the start of each new test, sufficient time (at least 1 hour) is allowed for the excess pore pressure generated from the previous test to dissipate by re-saturating the sample with a pore pressure of 4 MPa. This procedure is in order to eliminate any influence of residual pressures on the interpretation of the hydraulic pulse test results.
- 5) This test procedure was repeated from step 1) onwards to derive four separate sets of data at each stress condition.

In order to verify the reproducibility of the pressure response, four to six pulse tests were carried out for each loading condition. In total separate 22 measurements with different confining pressures and pulse sizes were carried out on the sample.

4.2.3.3 Steady State Method

For each confining stress, after performing the pulse tests for several times and re-equilibrating the pore pressure at least for 5 hours, the permeability of the cement sample was measured using a steady state technique in the same isothermal condition. The experiments were performed by fixing a different pore pressure between the upstream and downstream while monitoring the flow rate. This difference was around 100 kPa during measurements in order to maintain a near constant effective stress throughout the sample. The measured flow rates were used to calculate the permeability using Darcy's equation Eq. 1. The pore pressure difference is read from the Honeywell in-line pressure transducer directly. The volumetric flow rate is retrieved from both upstream pump and downstream pump (the flow rate should be the same from the two pumps).

4.3 Results and Data Reduction

4.3.1 Steady State Method

The steady state testing required more than two hours to stabilize to a constant steady state flow across the sample. For each test, the water flow rate is estimated as the resulting slope when plot syringe pump volume variation versus time. One test result is shown in Figure 50. The upstream and downstream water flow rate stabilized to a value of 2.07E-04 ml/min; the permeability was calculated based on this rate using Darcy's law. Table 7 gives the measured permeability values at different stress conditions.



(b) Downstream Flow Rate

Figure 50. Upstream and downstream syringe pump volume change throughout the steady state experiment

Test No.	Confinement (MPa)	Permeability* (m ²)				
1	7	3.64E-19				
2	8	3.29E-19				
3	9	3.25E-19				
4	10	3.28E-19				
5	11	3.12E-19				
*Note:	Two decimals are used for differentiating					
	the permeability values calculated from					
	steady state flow rate curve fitting					

Table 7 The results of steady state tests performed on the cement sample

The measured permeability determined with the constant-pressure steady-state tests varied between $3.12E-19 \text{ m}^2$ to $3.64E-19 \text{ m}^2$ for the cement sample. This is a narrow range.

4.3.2 Reservoir Volume and Compressive Storage

Figure 51 shows one set of volume measurement test data with nitrogen. The values of V_u and V_d (see Eq. 33) are 15 ml and 19 ml respectively and the average values of V_{u1} and V_{d1} are 5 ml and 7 ml. Thus the real reservoir volume that related to the pulse test would be $V_{u2} = V_u - V_{u1} = 10$ ml, $V_{d2} = V_d - V_{d1} = 12$ ml.



Figure 51. Upstream and downstream lines dead volume measurement results, the intercept of trendline in each graph is rounded to integer giving the volume measurement results: a) Vu = 15 ml; b) Vd = 19 ml; c) Vu1 = 5 ml; d) Vd1 = 7 ml.

Figure 52 and Figure 53 present one set data from a reservoir storage measurement. The time scale on the abscissa shows when the syringe pump A is injecting liquid into the system. The ordinate shows the hydraulic head difference due to the liquid injection to the downstream reservoir (1m H₂O = 98 kPa). The pressure change was measured directly with the in-line pressure transducer, because the in-line Honeywell pore pressure transducers are placed after the delivery valves, once the delivery valves are shut down, they are not able to detect the pressure changes before the valves. Thus for S_{u1} and S_{d1} measurement, the pump's built in pressure transducers must be used for recording pressure changes.

The slope of the experimental data shown in Figure 52 and Figure 53 corresponds to $\frac{dh}{dt}$ in Eq. 14 with the constant injection flow rate $\frac{dV}{dt} = 0.001$ ml/min. Then, the downstream reservoir storage from the pump outlet to the end of the sample, S_d equals 4.17E-10 m², from the pump outlet to the delivery valve, S_{d1} equals to 3.22E-10 m². The real downstream reservoir storage may be calculated with Eq. 34.

$$S_d = \frac{dV_d}{dh} = \frac{d(V_{d1} + V_{d2})}{dh} = \frac{dV_{d1}}{dh} + \frac{dV_{d2}}{dh} = S_{d1} + S_{d2}$$
 Eq. 34

Similarly, the upstream values are conducted with Pump B. In our calibration test strategy, we measured each parameter five times; the values of S_{d2} and S_{u2} required to complete the pulse test analysis, were obtained by subtracting the average value of these S_{d1} and S_{u1} from the average values of S_d and S_u . The results are summarized in Table 8.



Figure 52. Test No.1 Downstream Compressive Storage Sd measurement



Figure 53. Test No.1 Downstream Compressive Storage Sd1 measurement

Table 8 Reservoir compressive strorage measurement results

Test No.	$Sd(m^2)$	$\mathrm{Sd}_{1}(\mathrm{m}^{2})$	$Sd_2(m^2)$	$Su(m^2)$	$Su_1 (m^2)$	$Su_2(m^2)$
1	4.17E-10	3.22E-10		4.68E-10	3.95E-10	
2	4.11E-10	3.18E-10		4.71E-10	3.83E-10	
3	4.06E-10	3.15E-10		4.64E-10	3.87E-10	
4	4.03E-10	3.13E-10		4.57E-10	3.85E-10	
5	3.95E-10	3.08E-10		4.65E-10	3.89E-10	
Avg.*	4.07E-10	3.15E-10	9.14E-11	4.65E-10	3.88E-10	7.70E-11
*Note:	Two decir	mals are us	sed to diffe	erentiate th	ne storage v	values

estimated from linear curve fitting

4.3.3 Pulse Test Results

The pulse test conditions of confining and pore pressures are summarized in Table 9. Each individual test run lasted around 25 minutes with a further period required to allow for pressure homogenization. The measured water permeability is in the range of 0.2 to 0.28 microdarcy, while the specific storage range from 2.6E-6 to 3.5E-6 m⁻¹. For illustration, we present results here from only one test; all the other data curves are summarized in Appendix B. The data obtained from the transient pulse permeability tests are the variation of the upstream pressure and downstream pressures with time. Figure 54 (a) shows an example of the pressure pattern in the upper and downstream reservoirs. The curves in the form of a non-dimensional pressure $P(t)/P_0$ versus time for

cement sample are illustrated in Figure 54, (b). The hydraulic parameters, specifically the permeability and specific storage, and the compressive storage of the permeating system were back-calculated using an optimization routine based on Hsieh's solution. For comparison with the measured data, simulated curves are generated from Hsieh's analytical solution (Eq. 8) and the parameter values obtained from the individual test. These curves are also shown in Figure 54 (c). The permeability calculated based on Brace's expression are also presented and compared. With the Brace analysis, the permeability is found by plotting the pressure decay $ln[\Delta P(t)]$ versus time t which yields a straight line having a slope α (Figure 54 (d)). The permeability k can be directly determined from α by Eq. 7 with the upstream and downstream volumes (V_{u2} and V_{d2}) measured from the Chapter 4.2.3.1. These values are listed in Table 9.



Figure 54. Experimental results for permeability measurement under the conditions: Pc
= 11 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time

Pulse decay interpretation based on Brace's solution is relative easy. To back calculate the parameters from the pulse test experimental data with the general analytical solution developed by Hsieh et al. (1981), a parameter optimization method is introduced based on the solution of an inverse problem. The objective of the inverse problem is to quantify and minimize the difference between experimental data and the corresponding computed value in order to obtain unknown

parameters. The error, or objective, function, which quantifies the difference between the experimental data and the corresponding theoretical value, is given by Eq. 35.

$$fval = \sum_{i=1}^{M} \left\{ \left[\frac{P_{u/d}(t_i)}{\Delta P_0} \right] - \left[\frac{P_{u/d}(t_i)^*}{\Delta P_0} \right] \right\}^2$$
 Eq. 35

where M is the number of measured data points. The first term represents the normalized downstream or upstream pressure calculated theoretically at time t_i , the second term represents the measured data from the experiment at the same time. Since the analytical solution (Eq. 8) depends on the values of k, S_s , S_u and S_d , so to does the error function. Consequently, Eq. 35 can be rewritten as:

$$fval = \sum_{i=1}^{M} \left\{ \left[\frac{P_{u/d}(t_i)_{(k,S_s,S_u,S_d)}}{\Delta P_0} \right] - \left[\frac{P_{u/d}(t_i)^*}{\Delta P_0} \right] \right\}^2$$
 Eq. 36

This function is a highly nonlinear function of the four parameters and it's impossible to backcalculate all of them. Thus, experimental measurements and techniques are needed to calibrate the system parameters S_u and S_d . These are described in Chapter 4.2.3.1. Following this calibration, two of the variables (S_u and S_d) are fixed, so the problem is reduced to find the best pair of (k, S_s) with one minimization algorithm, which could be adopted for handling such general nonlinear functions. MATLAB code has available functions related to simplex optimization search method, which was adopted in this study to find the best pair of parameters.

To illustrate the transient pulse test with Hsieh's analytical solution, the above experimental result is taken as an example of how to obtain permeability step by step:

- 1) Plot the normalized pressure decay curve against time (Figure 54). Find out the final equilibrium ($P_{t=large}/P_0$) value. This is typically 0.33 for the experimental results presented here.
- 2) Because the final equilibrium value is governed by the first term on the right side in Eq. 8, which is a function of S_u , S_s and S_d . We fix the value of S_u that obtained from calibration test, which is 7.70E-11 m² (see in Chapter 4.2.3.1), use trial and error method to obtain the value of S_s to adjust the value of S_d within 5% range apart from 9.14E-11 m² that we measured from the calibration work.

The value of S_s has to be within the range of 1E-6 m⁻¹ to 1E-5 m⁻¹ based on our initial guess of specific storage from the Chapter 4.2.1. Thus we could obtain the initial guess value of S_u , S_s and S_d

- 3) Using the MATLAB program with the built in simplex search function, input the necessary parameters including S_u and S_d , search for the best pair of S_s and k to minimize the error (Eq. 36) over a feasible domain in parameter space. Based on the initial parameter guess from step 2) and Brace's solution, we restrict the search range for S_s is 1E-6 to 1E-5 m⁻¹ and the range for k is 1E-20 to 1E-18 m². However, currently the program is not time efficient enough to enlarge the search range for S_s , but the fitted results for permeability interpretation are good enough for field practical applications.
- 4) Thus a suite of parameters is obtained. In the end of the interpretation, the fitted curve is calculated using the analytical solution shown in Figure 54. The simulated curves for pressure decay of the specimen are in close agreement with the experimental results. These results demonstrate the effectiveness and accuracy of the proposed approach for back-calculating hydraulic parameters of the specimen and reservoir storage of the equipment from a transient-pulse test.

In fact, it's difficult to reconcile both upstream and downstream curves with the same set of parameters. In the study, upstream and downstream pressure curves are used to conduct the analysis independently. But the calculated parameters are similar. The small discrepancy hints that the sample is not perfectly homogenous despite its macroscale appearance; the cement sample can be heterogeneous and anisotropic at the micro-scale. The heterogeneity may be entirely due to the specific storage, the different distribution of specific storage between the top and bottom of the sample may be the primary reason causing calculated permeability different for the upstream and downstream curves. As already stated, the identification mainly focuses on intrinsic permeability, the sample's storage factor is also identified simultaneously, albeit with less certainty.

A comparison of the calculated permeabilities (Table 9) shows that the permeability interpreted using Brace's method tends to under-estimate the permeability compared to Hsieh's solution. However, the discrepancy only is around 1E-19 m² which is acceptable for general engineering applications. This suggests that the simple Brace's method permits a good level of approximation of the permeability for the cement samples. The validity of using the Brace et al.

(1968) solution for permeability determination depends on many factors, including the specific storage and the dimensions of the specimen and the individual reservoir volumes. Brace et al. (1968) state that their solution is valid when the fluid storage capacity of a specimen is negligible compared with that of reservoir system. However, this solution can still be employed with reasonable accuracy.

When interpreting the pulse test data, Brace's method could be used initially, to obtain a first permeability estimation range for the Hsieh's solution interpretation. Nevertheless, Hsieh's solution is more accurate, and should be carried out as a second step when dealing with more critical issues such as analyzing cement pore pressure diffusive response under SAGD thermal effect. The analytical interpretation allows the identification of the storage factor of the sample and reservoirs, meaning the true physics of the test can be taken into account. This results in a more rational set of parameter values. In this way, it gives an improvement in parameter values when compared with Brace's method.

The permeability estimated from the hydraulic pulse tests performed on the same sample were within the range 2.17E-19 m² to 2.82E-19 m² which also is within the same order of magnitude of the results obtained from the steady state tests. The steady state test results showed higher permeability values, the same as most researchers' findings (Boulin et al. 2012; Selvadurai and Najari 2015; Selvadurai and Najari 2016). It can thus be concluded that the pulse technique compares well with the steady state method of permeability measurement.

		D	Brace's Solution	Hsieh's Solution									
Test No.	st No. Confinement (MPa)			Fit Downstream					Fit Upstream				
		increment (MPa)	Permeability* (m ²)	Permeability* (m ²)	Su (m ²)	$Sd(m^2)$	Ss (m ⁻¹)	fval	Permeability* (m ²)	Su (m ²)	Sd (m ²)	Ss (m ⁻¹)	fval
1	11	0.375	1.18E-19	2.66E-19	7.70E-11	9.16E-11	2.70E-06	0.0025	2.47E-19	7.7E-11	9.16E-11	2.60E-06	0.0165
2	11	0.368	1.18E-19	2.63E-19	7.70E-11	9.18E-11	2.90E-06	0.0175	2.61E-19	7.7E-11	9.18E-11	2.70E-06	0.0088
3	11	0.374	1.15E-19	2.63E-19	7.70E-11	9.18E-11	2.80E-06	0.0139	2.50E-19	7.7E-11	9.18E-11	2.70E-06	0.0126
4	11	0.369	1.18E-19	2.58E-19	7.70E-11	9.13E-11	2.70E-06	0.0093	2.49E-19	7.7E-11	9.13E-11	2.70E-06	0.0274
5	11	0.612	1.15E-19	2.76E-19	7.70E-11	9.16E-11	3.20E-06	0.0037	2.34E-19	7.7E-11	9.16E-11	3.10E-06	0.0110
6	11	0.608	1.15E-19	2.76E-19	7.70E-11	9.16E-11	3.00E-06	0.0118	2.35E-19	7.7E-11	9.16E-11	2.90E-06	0.0036
7	10	0.365	1.15E-19	2.76E-19	7.70E-11	9.19E-11	3.00E-06	0.0159	2.35E-19	7.7E-11	9.19E-11	2.90E-06	0.0052
8	10	0.377	1.15E-19	2.63E-19	7.70E-11	9.14E-11	2.80E-06	0.0044	2.48E-19	7.7E-11	9.14E-11	2.70E-06	0.0180
9	10	0.368	1.15E-19	2.82E-19	7.70E-11	9.13E-11	3.50E-06	0.0092	2.16E-19	7.7E-11	9.13E-11	3.50E-06	0.0062
10	10	0.363	1.15E-19	2.61E-19	7.70E-11	9.17E-11	2.90E-06	0.0097	2.35E-19	7.7E-11	9.17E-11	2.90E-06	0.0100
11	9	0.363	1.18E-19	2.78E-19	7.70E-11	9.14E-11	3.30E-06	0.0089	2.26E-19	7.7E-11	9.14E-11	3.10E-06	0.0071
12	9	0.362	1.18E-19	2.78E-19	7.70E-11	9.15E-11	3.00E-06	0.0136	2.20E-19	7.7E-11	9.15E-11	3.00E-06	0.0136
13	9	0.360	1.12E-19	2.82E-19	7.70E-11	9.16E-11	3.30E-06	0.0058	2.19E-19	7.7E-11	9.16E-11	3.30E-06	0.0058
14	9	0.365	1.15E-19	2.66E-19	7.70E-11	9.20E-11	2.90E-06	0.0144	2.35E-19	7.7E-11	9.20E-11	2.80E-06	0.0099
15	8	0.353	1.12E-19	2.82E-19	7.70E-11	9.15E-11	3.50E-06	0.0058	2.09E-19	7.7E-11	9.15E-11	3.50E-06	0.0278
16	8	0.365	1.12E-19	2.56E-19	7.70E-11	9.16E-11	2.80E-06	0.0049	2.20E-19	7.7E-11	9.16E-11	2.80E-06	0.0135
17	8	0.359	1.12E-19	2.76E-19	7.70E-11	9.13E-11	3.30E-06	0.0168	2.11E-19	7.7E-11	9.13E-11	3.40E-06	0.0102
18	8	0.362	1.12E-19	2.50E-19	7.70E-11	9.14E-11	2.90E-06	0.0175	2.35E-19	7.7E-11	9.14E-11	2.90E-06	0.0094
19	7	0.368	1.06E-19	2.58E-19	7.70E-11	9.13E-11	3.20E-06	0.0138	2.18E-19	7.7E-11	9.13E-11	3.10E-06	0.0082
20	7	0.366	1.09E-19	2.82E-19	7.70E-11	9.19E-11	3.50E-06	0.0130	2.03E-19	7.7E-11	9.19E-11	3.50E-06	0.0124
21	7	0.354	1.09E-19	2.66E-19	7.70E-11	9.19E-11	3.10E-06	0.0039	2.20E-19	7.7E-11	9.19E-11	3.00E-06	0.0117
22	7	0.359	1.09E-19	2.49E-19	7.70E-11	9.17E-11	2.90E-06	0.0107	2.17E-19	7.7E-11	9.17E-11	2.90E-06	0.0125

Table 9 Parameters back-calculated from the transient pulse test of cement under different confining pressures based on

Brace's and Hsieh's method (initial pore pressure was kept at 4 MPa)

*Note: Two decimals are used to differentiate permeability values interpreted from curve fitting and back calculation

4.3.4 Numerical Simulation with COMSOL Multiphysics

In order to simulate the cement pore pressure decay behavior that observed in a hydraulic pulse test, a typical one-dimensional test configuration was modelled and analyzed using the Multiphysics finite element code COMSOLTM. In COMSOL, the software offered the Heat Transfer Module is equivalent to the physical model used for current transient pore pressure diffusion simulation. The theory for heat transfer in solids can be simplified in the following piezo-conduction equation:

$$\frac{\partial^2 T}{\partial x^2} - \frac{\rho C_p}{k} \frac{\partial T}{\partial t} = 0$$
 Eq. 37

where *T* is the absolute temperature (*K*), ρ is the density (kg/m³), C_p is the specific heat capacity at constant stress (J/(kg·K)), *k* is the thermal conductivity (W/(m·K)). Note the similarities between the flow of fluid (Eq. 2) and the heat flow through the solids (Eq. 37); the difference is in heat diffusion equation, temperature response is analyzed, while in pulse test, pore pressure response is the key physical parameter. Though the associated parameters have different definitions in different areas of physics, basically they have similar meaning. For example, in pulse test we use hydraulic conductivity, while in the heat transfer module thermal conductivity is used. Nevertheless, because of the close correlation of these two properties, we infer that heat flowing through the solids under a potential gradient follow the same path as particles of water flowing under a pressure gradient. Thus if we could correlate the parameters obtained from the parameter identification method with the input parameters necessary in COMSOL simulator with the same boundary conditions, we could simulate the pulse decay behavior with the finite element modeling technique.

Figure 55 shows the geometry and boundary conditions of the problem. Initially the pressure or temperature within sample and reservoirs domain all equal to 0. The pulse was initialized by applying the "Temperature" boundary condition in the software within upstream reservoir, T = 100 °C. The model inputs for No.1 and No.2 experiments are summarized in Table 10 and Table 11. All parameters remain constant over the experiment. The measured reservoir pressure curves and the computed normalized temperature diffusion curves are shown in Figure 56.



Figure 55. The geometry and boundary conditions of the one-dimensional hydraulic pulse test

Table 10 COMSOL parameters used for simulations of the transient methods for Test

No.	1
-----	---

Madal Innuta	Unit	Simula	tion of Down	stream	Simulation of Upstream			
woder inputs		Upstream	Downstream	Sample	Upstream	Downstream	Sample	
k	W/(m·K)	2.00E+07	2.00E+07	3.97E+03	2.00E+07	2.00E+07	3.68E+03	
C _p	J/(kg·K)	7.60E+06	7.70E+06	3.30E+06	7.60E+06	7.70E+06	3.20E+06	
ρ	kg/m ³	1000	1000	1000	1000	1000	1000	

Table 11 COMSOI	parameters used for	r simulations of the	e transient methods for	Test
-----------------	---------------------	----------------------	-------------------------	------

No.2

Model Innute	I Init	Simulation of Downstream			Simulation of Upstream			
woder inputs	UIII	Upstream	Downstream	Sample	Upstream	Downstream	Sample	
k	W/(m·K)	2.00E+07	2.00E+07	3.92E+03	2.00E+07	2.00E+07	3.89E+03	
C _p	J/(kg·K)	7.60E+06	7.65E+06	3.60E+06	7.60E+06	7.65E+06	3.40E+06	
ρ	kg/m ³	1000	1000	1000	1000	1000	1000	



(b) Test No.2

Figure 56. COMSOL simulation results compared with experimental data

To illustrate the numerical modeling method in detail, Test No.1 is taken as an example of how to obtain the model inputs parameters in detail. From the analytical method, the permeability value from the downstream data analysis is $2.66E-19 \text{ m}^2$, the hydraulic conductivity is then calculated as Eq. 38 equal to 3.97E-12 m/s.

$$K^* = \frac{\gamma_w}{\mu_w} k^*$$
 Eq. 38

where K^{*} and k^{*} are hydraulic conductivity and permeability. (Note: we use the "*" in hydraulic sector to differentiate from the thermal equation)

Compare the Eq. 37 with Eq. 2, ρC_p is similar to the function of S_s in the pulse test, but it doesn't have to make ρC_p exactly equal to S_s and thermal conductivity equal to hydraulic conductivity. As long as $\frac{\rho C_p}{k} = \frac{S_s}{K^*}$, the simulation would be fine. The ratio of specific storage (2.7E-6 m⁻¹) over hydraulic conductivity, is:

$$\frac{S_s}{K^*} = \frac{2.7E - 6}{3.97E - 12} = \frac{2.7E6}{3.97}$$
 Eq. 39

We keep the density for reservoir and sample domains all the same as 1000 kg/m³ to simplify the problem, initial guess for C_p and thermal conductivity k could be 2.7E6 and 3.97E3.

The reservoir specific storage using the similar definition as S_s could be calculated as:

$$S_{us} = \frac{S_u}{V_u} = \frac{7.70E - 11m^2}{10E - 6m^3} = 7.7E - 6m^{-1}$$
 Eq. 40

$$S_{ds} = \frac{S_d}{V_d} = \frac{9.16E - 11m^2}{12E - 6m^3} = 7.6E - 6m^{-1}$$
 Eq. 41

Since the reservoir specific storage has the same order of magnitude as the sample's S_s , we use the same order as well in the simulation inputs for C_p . To apply the pulse, the conductivity in the reservoir domain should be set as large as possible to make sure the temperature could be transmitted very fast across the reservoir. Here we gave the value of 2E7 to simulate the reservoir conductivity. This value is not fixed and only has the influence on the initial pulse application behavior.

With all the initial guess for value of C_p and k, the program is ready to run. The first results showing that the final equilibrium value has relative large discrepancy, not the time needed to reach the equilibrium. Thus it's reasonable to adjust the value of C_p who governs the final equilibrium. Then we fix the value of k and adjust C_p only within 10%, then the best pair of parameters could be obtained in COMSOL when the calculated curve is closest to the experimental curve. Thus when compare the results of parameters, only the sample's specific storage is different from the analytical method. Actually the discrepancy between the numerical results and analytical results may not be that large, because the final equilibrium should be governed also by the reservoir storage. There might be large experimental error in the reservoir volume measurement in the calibration test, causing the error of the estimation of reservoir storage. But we neglect this errors to simplify the searching procedure, we fix all the other variables, only adjust the value of the sample specific storage. Overall the numerical results are comparable with the analytical results confirming the pulse decay characteristic of cement sample, except the sample's specific storage. This technique is useful in the future study to analyze the heterogeneity of the permeability in any kind of tight geomaterial.

4.3.5 Main Uncertainties

There are many issues raised in pulse decay tests, here are several main factors that resulting in the main uncertainties when interpreting the experimental data. The way to minimize the uncertainties with optimal and practical experimental strategies are presented above and repeated here. The equipment calibration and validation stages are key steps to evaluate and control the uncertainties inherent to any experimental equipment. However, a detailed study investigating the influence of all the uncertainties mentioned in this section theoretically or analytically are not within the scope of the thesis, but it's highly recommended in the future study.

1) The main parameters that could influence the measurement of the flow rate and fluid pressure are the leakage through the fittings, the sealing condition and the accuracy of the electronic devices measuring the parameters. The leakage through fittings, estimated by performing series of blank runs with impermeable samples. The influence of the leakage on the estimation of permeability was considered to be negligible. Regarding the accuracy of the electronic devices used in the experiments, the pressure transducer measured the pressure to within an accuracy of $\pm 0.25\%$ of the full range, i.e. ± 50 kPa, this may produce some errors in the estimation of the permeability.

- 2) The degree of saturation of the pore space and the system can have a significant influence on the estimation of permeability even when performing steady state tests (Selvadurai & Ichikawa 2013). A key assumption in theoretical developments related to permeability testing in general is that the accessible pore space through which the flow takes place is fully saturated. It is important to note that absence of full saturation of the accessible pore space can lead to erroneous interpretations of permeability derived from both steady state and transient tests. While every precaution is taken to remove air and saturate the pressurised system with water, the presence of air cannot be completely eliminated, even in experiments conducted in a controlled laboratory environment. The air fraction may be introduced through either the experimental procedure, air can be trapped as bubbles that stick to the inner surfaces of the fittings or released from the unsaturated regions of the porous medium because cement sample is naturally air entrained, with air contents in the range of five to six percent. The presence of air has a direct influence on the compressibility of the fluid in the pressurised region and its effective compressibility is also influenced by the applied pressure. In turn, different amount of air trapped in the system between calibration test and pulse test will cause inaccurate determination of the reservoirs compressive storage. Selvadurai and Najari (2015) present an analytical approach to investigate the potential role of the air fraction. It is shown that if the effect of the air fraction is omitted from the analysis of the hydraulic pulse test, the permeability can be underestimated.
- 3) In the test procedure, the downstream delivery valve were closed prior to the test and the upstream valve was used to apply the pulse. Different volumes of water was transferred through the valves between each test, thus producing different values of the reservoir compressive storage. This factor is hard to quantify in the calibration test. In fact, the pulse test main uncertainties rely on S_u and S_d , which have a direct impact on (k, S_s) . Combining the effect of the entrapped air mentioned above, when interpreting the pulse test data, the value of S_u which measured from the calibration test was fixed, only adjust the value of S_d within 5% to account for the variation.

- 4) Experimental approach for saturating the sample and pulse test itself will let the sample contain a residual hydraulic gradient immediately afterwards. The residual hydraulic gradients can also influence the pulse decay observed in a one-dimensional hydraulic pulse test and consequently the interpretation of the permeability of the material (Selvadurai 2009). Pore pressure is assumed initially constant in pulse tests. It is thus important to wait sufficient time for the residual hydraulic gradients to dissipate and pressure stabilization within the sample prior to any transient tests. However, it is impossible to know for sure when pressure stabilization is done within the sample because the time required for the complete dissipation of the residual hydraulic gradient will depend on the permeability of the porous medium which unfortunately is the property that is being sought. In our test, the cement sample needs 25 mins to dissipate the pressure pulse, thus at least one hour was needed for re-saturation at the same pore pressure vale to eliminate this effect as much as possible.
- 5) The accurate estimation of the initial pore pressure induced by the pulse at the upstream reservoir, ΔP_0 . Although the 1 sec recording rate was used to monitor the pressure evolution, (to the author's knowledge, the highest rate that compared to the publications related pulse test programs), it's difficult to capture the accurate magnitude of the pulse increase ΔP_0 due to the too rapid pressure decay. The inaccurate value of ΔP_0 will affect the normalized pressure value that used in the analytical approach interpretation as the experimental input, thus affecting the parameters that obtained from the pulse test especially for the procurement of S_s which is fully dependent on the normalized final equilibrium value. Uncertainties on S_s will further lead to uncertainties on k.
- 6) In our analysis, the pulse decay test is assumed as a 1D pressure evolution process, however the motion of water in pulse decay test is not a 1D process, and its interpretation as such can lead to more uncertainties. In fact, during pulse decay, pore pressure is non uniform within the sample and changes with time. The lateral deformation of the sample, due to effective pressure change, involves motion of water. Thus it's a fully poromechanical coupled problem, and consequently, Giot et al. (2010) conducted finite element method with 2D-axisymmetrical homogenous porous media numerical model and back calculate the permeability, Biot coefficient, drained Young's modulus and reservoir compressibility. They stated interpreting pulse decay signal as a 1D problem, as Hsieh et al. (1981) also the

method used in the thesis, can lead to major uncertainties on the specific storage S_s of the sample but lower uncertainties on the permeability value. Thus two-dimensional back analysis is more accurate than 1D analysis, which nevertheless gives good approximate parameters.

4.4 Discussion and Summary

4.4.1 Steady State vs. Pulse Test

Figure 57 compares the cement permeability values characterized by using steady state, pulse test methods based on Hsieh's and Brace's solution. The permeability estimated from the hydraulic pulse tests performed on the same sample were within the range 2.17E-19 m² to 2.82E-19 m² which also is within the same order of magnitude of the results obtained from the steady state tests. The steady state test results showed higher permeability values, the same as most researchers' findings (Boulin et al. 2012; Selvadurai and Najari 2015; Selvadurai and Najari 2016). It can thus be concluded that the pulse technique compares well with the steady state method of permeability measurement.

The permeability determined from the constant pressure steady state is considered to be the most reliable data within these methods since no other constitutive parameters related to the mechanical behaviour of the rock are involved in the estimation of permeability. For these testing conditions, the permeability did not decrease with the increase of the hydrostatic loading. A consistent observation in these tests is the slight reduction in the permeability of the material for each subsequent test conducted on the sample. It is difficult to pin point any specific reason for this phenomenon. Possible influences could include the following: the complete saturation of the cement paste leading to a hydration of the cement and a subsequent reduction in the pore space; the elimination of the Klinkenberg effect due to the progressive increase in the saturation of the pore space; chemical reactions between the water and the cement leading to the precipitation of leached material in the pore space, resulting in a reduction in the permeability.



Figure 57. Permeability of cement at different stress state in different test methods

4.4.2 Concluding Remarks

Permeability of oil well cement is an important parameter for conducting large scale or small scale wellbore integrity numerical analysis. As a scale-dependent parameter, wellbore cement permeability can be influenced by operational procedures which can either enhance or impede the flow of fluids through the medium. For this reason, permeability evaluation should ideally be conducted in situ. However, the laboratory evaluation provides a useful benchmark for preliminary feasibility assessments of complex wellbore design solutions. Laboratory estimation of permeability can be carried out using either steady state or transient techniques on cylindrical samples, but for low w/c ratio cement, it's not efficient and feasible to use steady state approach due to the prolong testing time. This chapter documents a novel laboratory experimental procedure which can be performed to determine the permeability characteristics of low permeability materials such as cement using both steady state and transient pulse test methods. The transient pressure pulse technique can be performed relatively quickly in comparison with many steady state test procedures advocated in the literature. The water permeability of class G well cement with these two methods were both determined and compared, the values were found to correlate very well. The small discrepancy observed in the absolute values of permeability could be due to several

uncertainties related to pulse test presented in this chapter as well. However, detailed analytical investigation on each factor is beyond the scope of the study. Overall, proper design and careful calibration of the apparatus, individual test design can eliminate uncertainties and increase the measurement accuracy. The results of the current experimental investigations give a reasonably repeatable set of data. Such data can be readily used to estimate the permeability characteristics of the cement, which are comparable with the results for permeability of cement paste available in the literature. Admittedly, a direct comparison of the sets of data is unwarranted in view of the possible differences between the two materials.

In terms of the interpretation of the pulse test, Brace's expression neglecting the sample's specific storage tends to give lower calculated permeability values compared to the more restrictive parameter identification method based on Hsieh's analytical solution. Furthermore, results from Hsieh's solution shows better agreement with steady state tests than Brace's expression, but Brace's method is still good enough for practical application because it's simpler and faster to use in the engineering world. The inverse method based on analytical solution consists of an error function that quantifies the differences between reservoir pressures that were measured and calculated. The calculated pressures are functions of the parameters to be identified. The simple Brace's expression for calculating permeability, assuming no specific storage within the sample and rigid reservoirs are not thoroughly accurate and tend to underestimate the permeability, because the specific storage is also an important parameter that controls unsteady flow in lowpermeability media and which affects the pressure responses produced during a pulse test. Therefore this property should not be ignored or considered insignificant in laboratory measurements. However, the results with Brace's solution could give an initial guess of the feasible permeability domain. In the end, the results of the experiments can be modelled accurately using a standard finite element software based on the parameter identification results. The first results of the test are quite encouraging and the method will be used for many more tests in the future. The factor that contributes to the discrepancy between experimental back-analysis and computational results is identified as experimental error in calibrating the reservoir compressive storage. The pulse test results clearly show a heterogeneity of the material. Thus it seems of fundamental importance to account for this heterogeneity in further work. Currently we only brought up the 1D homogeneous finite element model in the analysis, the 2D fully coupled

inversion of the pulse test is maybe necessary for studying heterogeneous in the permeability characteristics.

5 Conclusions and Recommendations

5.1 Conclusions

A review of literature concerned with wellbore integrity, specifically caprock integrity and cement sheath for zonal isolation is conducted and the need for a careful characterization of the caprock and cement properties is emphasized. This thesis establishes experimental investigation work for a future large-scale fully coupled reservoir-geomechanical numerical analysis to assess the possible range of behaviors that may occur when the wellbore encounter the steam chambers. The work reported in this thesis focuses on the caprock hydro-mechanical properties characterization and well cement permeability measurement in the context of the SAGD wellbore integrity analysis.

Two experimental programs were conducted and discussed in detail. First is the characterization of Wabiskaw shale hydro-mechanical properties. It was the purpose of the present investigation on the hydraulic and mechanical behavior of the two undisturbed Wabiskaw caprock core samples to fill one of the major gaps in the present knowledge of caprock hydro-mechanical phenomena, by looking into the drained isotropic consolidation tests and steady state permeability tests, and to develop a general description of constitutive behavior based on the available constitutive model and empirical equations. Experimental equipment plays a major role in the successful performance of long duration consolidation tests. A special testing system was developed. Microscale approach was also applied for analyzing caprock creep behavior. Major conclusions from this experimental investigation are as follows:

Because the major caprock geomechanical behaviors during the SAGD process involve the isotropic loading and unloading process, a representative geomechanical constitutive model of the Wabiskaw shale, the modified Cam-clay model, was obtained based on two numerical experiments, which match laboratory results under the same testing conditions. The proposed model parameters, such as the slope of swelling line and normal consolidation line can be applied in the future coupled reservoir geomechanical simulations of the SAGD process.

- The results showed that the loading-unloading process does induce caprock parameter variations, particularly the absolute permeability. The phenomenological equations proposed by David et al. (1994) have been proposed for describing permeability evolution as a function of effective stress and porosity which are applicable to Wabiskaw shale formation with high and low clay content. These results yield significant insight into the hydro-mechanical properties of caprock.
- During the consolidation test, it is apparent from the data that Wabiskaw experienced significant creep behavior. An open discussion was initiated by providing several possible microscale level processes that might cause the macroscopic behavior. Among others, key findings when examining the creep mechanisms of this caprock is their inherent self-sealing capacity indicating that Wabiskaw shale enables maintenance of low rock mass permeabilities as well as accommodating deformations without extensive damage and rupture.
- The non-linear volume reduction during isotropic compression enables the estimation of pressure-dependent compressibility and Biot coefficient of this material. The results show the decrease of compressibility with effective stress increase. The Biot coefficient decreases with effective stress increases.

Second is to experimentally investigate the cement permeability using the transient pulse decay technique. The pulse test has great utility in conducting relatively rapid tests to investigate the permeability characteristics of low-permeability materials. Experimental apparatus design is of fundamental importance to ensure the sensitivity of a pulse test experiment with respect to the permeability and specific storage coefficient of the sample. Water permeability measurements are conducted on one Class-G cement sample, after the curing periods of 28 days. A series of experimental studies and theoretical analyses on calculating permeability from the pulse test have been presented. Two different pulse-decay calculation methods for cement permeability are utilized and compared. Based on the work completed, the following conclusions are made and summarized:

• Validity of a back analysis parameter identification method based on Hsieh's solution to the pulse test was confirmed by comparing the obtained permeability with the permeability values obtained from steady state method, the values from the pulse test are

in very good agreement with the steady state permeability data. The capability of this testing system characterizing the cement permeability is well demonstrated through over twenty times pulse tests.

- Comparing Brace's solution and Hsieh's solution to the pulse test, Hsieh's method incorporates the both specimen and reservoir system compressive storage effect, thus providing an obvious enhancement for the contribution of the storage effect in permeability calculation and a good direction of how the true physical process involved in the pulse decay behavior. The results confirm the need for taking into account all necessary parameters into the pulse test interpretation.
- To accurately calculate the permeability and the specific storage of the specimen, the values of compressive storage for the fluid reservoirs should be determined experimentally by an independent calibration test.
- The association of the experimental results with finite element modelling results in a better understanding of the phenomena and physics that take place during the pulse test. The capacity of parameter prediction under specified conditions and the better understanding of the decay behavior of the tests, clearly demonstrate the advantages.
- In principle, pulse test is a sound test; however, there are a number of factors that can influence the pressure pulse decay. An appropriate experimental set up and procedure could help control the uncertainties (entrapped air, compressive storage etc.) associated with the pulse decay tests.

5.2 Recommendations for Future Work

The hydro-mechanical characterization study of Wabiskaw shale formation was first presented in the thesis. The experimental campaign along with the numerical modeling require more investigation to accommodate more complicated downhole conditions and reservoir stress path. Based on the experimental output discussed in the previous section, the following recommendations may be useful for future studies of geomechanical assessment for caprock.

• For caprock consolidation test, the temperature was set still pretty low compared to the real SAGD in-situ condition. More research is needed to evaluate the thermal-hydro-

mechanical behavior of the caprock to fully characterize the constitutive behavior of caprock under the simulated SAGD operation condition.

- The creep behavior of caprock are so far poorly understood, the investigation on the effect of creep behavior on caprock coupled reservoir-geomechanical anlaysis is not found in literature at present. Therefore, further experimental investigations are still needed and efforts are required to investigate the mechanism of caprock creep at different stress states, similar to the SAGD reservoir in-situ condition. Specifically, differential creep test should be carried out on caprock clay shale samples to examine the creep constitutive model.
- In Chapter 3.3.4, only three poroelastic parameters were identified. Studies on poromechanical properties of caprock are insufficient. A complete set of poroelastic parameters of the caprock material is recommended to characterize through the experimental study consisted in drained, undrained and unjacketed isotropic compression tests.

In the second part of the thesis, several questions were produced that need to be answered. More work should be conducted on these questions in future research in order to better understand transient pulse test technique and fluid transport properties within cement. The most relevant recommendations are shown as below:

- The purpose of Chapter 4 is to calibrate the pulse test system, thus the cement sample curing strategy is the simplified one without any high temperature and high pressure applied when curing the cement sample. To simulate cement behavior under the downhole condition, more strict and specific curing strategy is required in the future research work. To better investigate the cement hydraulic integrity in the long-term, permeability test should be conducted under high temperature environment lasting for a long period.
- There are several uncertainties mentioned in the pulse test implementation and interpretation, investigating their influence on the performance of the pulse test are highly recommended in the future study.
- Further study should be designed to investigate and accommodate the effects of the hydraulic orthotropy and hydraulic heterogeneity in the geo-material flow modeling problems combining with the experimental pulse test technique and finite element modelling.

• In the field of SAGD reservoir, the wellbore has to cope with heating due to the steam injection (the maximal temperature to be reached is 350 °C). It is worthwhile to study the influence of temperature on water permeability within hardened cement pastes to simulate the SAGD thermal process.

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Appendix A: Experimental Data from Wabiskaw Isotropic Consolidation



Figure A.1 1st Sample volumetric strain – log time relation at 2.15 MPa



Effective Mean Stress = 4.15 MPa



Effective Mean Stress = 6.13 MPa



Figure A.3 1st Sample volumetric strain – log time relation at 6.13 MPa



Effective Mean Stress = 8.11 MPa



Time (min) 1 10 100 1000 10000 100000 0 -----Initial Compression 0.005 Primary Consolidation Volumetric Strain 0.01 0.015 0.02 0.025 Secondary Consolidation 0.03

Effective Mean Stress = 10.94 Mpa

Figure A.5 1st Sample volumetric strain – log time relation at 10.94 MPa



Effective Mean Stress = 2.18 MPa

Figure A.6 2nd Sample volumetric strain – log time relation at 2.18 MPa







Figure A.7 2nd Sample volumetric strain – log time relation at 3.18 MPa



Effective Mean Stress = 4.18 MPa

Figure A.8 2nd Sample volumetric strain – log time relation at 4.18 MPa

Effective Mean Stress = 5.18 MPa



Figure A.9 2nd Sample volumetric strain – log time relation at 5.18 MPa



Effective Mean Stress = 7.17 MPa

Figure A.10 2nd Sample volumetric strain – log time relation at 7.17 MPa

Effective Mean Stress = 9.18 MPa



Figure A.11 2nd Sample volumetric strain – log time relation at 9.18 MPa



Effective Mean Stress = 11.17 Mpa



Effective Mean Stress = 13.16 MPa



Figure A.13 2nd Sample volumetric strain – log time relation at 13.16 MPa



Effective Mean Stress = 15.16 MPa

Figure A.14 2nd Sample volumetric strain – log time relation at 15.16 MPa

Effective Mean Stress = 17.15 MPa



Figure A.15 2nd Sample volumetric strain – log time relation at 17.15 MPa



Effective Mean Stress = 19.15 MPa

Figure A.16 2nd Sample volumetric strain – log time relation at 19.15 MPa

Appendix B: Permeability Data for Well Cement



(b)



(d)

Figure B.1 Test No.2 results for permeability measurement under the conditions: Pc = 11 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.2 Test No.3 results for permeability measurement under the conditions: Pc = 11 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.3 Test No.4 results for permeability measurement under the conditions: Pc = 11 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



Figure B.4 Test No.5 results for permeability measurement under the conditions: Pc = 11 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



Figure B.5 Test No.6 results for permeability measurement under the conditions: Pc = 11 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



Figure B.6 Test No.7 results for permeability measurement under the conditions: Pc = 10 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



Figure B.7 Test No.8 results for permeability measurement under the conditions: Pc = 10 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.8 Test No.9 results for permeability measurement under the conditions: Pc = 10 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.9 Test No.10 results for permeability measurement under the conditions: Pc = 10 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



Figure B.10 Test No.11 results for permeability measurement under the conditions: Pc = 9 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.11 Test No.12 results for permeability measurement under the conditions: Pc = 9 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)


(d)

Figure B.12 Test No.13 results for permeability measurement under the conditions: Pc = 9 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.13 Test No.14 results for permeability measurement under the conditions: Pc = 9 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.14 Test No.15 results for permeability measurement under the conditions: Pc = 8 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.15 Test No.16 results for permeability measurement under the conditions: Pc = 8 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.16 Test No.17 results for permeability measurement under the conditions: Pc = 8 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.17 Test No.18 results for permeability measurement under the conditions: Pc = 8 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.18 Test No.19 results for permeability measurement under the conditions: Pc = 8 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.18 Test No.19 results for permeability measurement under the conditions: Pc = 8 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.19 Test No.20 results for permeability measurement under the conditions: Pc = 7 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.20 Test No.21 results for permeability measurement under the conditions: Pc = 8 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time



(b)



(d)

Figure B.21 Test No.22 results for permeability measurement under the conditions: Pc = 8 MPa and Pp = 4 MPa (a) pressure decay vs. time; (b) normalized pressure decay vs. time; (c) simulated curve compared with experimental data; (d) natural logarithm of normalized differential pressure across the specimen vs. time