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**SHEAR-FLOW CHARACTERISTICS OF ROCK
FRACTURES AND IMPLICATIONS FOR
INJECTION-INDUCED SEISMICITY**

JI YINLIN

SCHOOL OF CIVIL AND ENVIRONMENTAL ENGINEERING

2020

SHEAR-FLOW CHARACTERISTICS OF ROCK FRACTURES AND IMPLICATIONS FOR INJECTION-INDUCED SEISMICITY

JI YINLIN

School of Civil and Environmental Engineering

A thesis submitted to the Nanyang Technological University
in partial fulfilment of the requirements for the degree of
Doctor of Philosophy

2020

Statement of Originality

I hereby certify that the work embodied in this thesis is the result of original research, is free of plagiarized materials, and has not been submitted for a higher degree to any other University or Institution.

24. Sep. 2020

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A handwritten signature in black ink, consisting of stylized, flowing characters that appear to be 'Ji Yinlin'.

JI YINLIN

Supervisor Declaration Statement

I have reviewed the content and presentation style of this thesis and declare it is free of plagiarism and of sufficient grammatical clarity to be examined. To the best of my knowledge, the research and writing are those of the candidate except as acknowledged in the Author Attribution Statement. I confirm that the investigations were conducted in accord with the ethics policies and integrity standards of Nanyang Technological University and that the research data are presented honestly and without prejudice.

25 Sep 2020

Date

WU WEI

Authorship Attribution Statement

This thesis contains the material from 2 papers published in peer-reviewed journals where I am the first author.

Chapter 4 is published as Ji, Y. and Wu, W. (2020). "Injection-Driven Fracture Instability in Granite: Mechanism and Implications", Tectonophysics, 791: 228572.

The contributions of the co-authors are as follows:

- ✧ A/Prof Wu Wei provided the initial project direction and edited the manuscript drafts.
- ✧ I co-designed the study with A/Prof Wu Wei, carried out the test, performed the data analysis, and drafted the manuscript.

Chapter 5 is published as Ji, Y., Wanniarachchi, W. A. M. and Wu, W. (2020). "Effect of Fluid Pressure Heterogeneity on Injection-Induced Fracture Activation", Computers and Geotechnics, 123: 103589.

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- ✧ A/Prof Wu Wei provided the initial project direction and edited the manuscript drafts.
- ✧ I co-designed the study with A/Prof Wu Wei, carried out the test, and analyzed the data.
- ✧ Dr. Ayal Wanniarachchi assisted in the development of numerical model in COMSOL.
- ✧ I wrote the manuscript draft. The manuscript was also revised by Dr. Ayal Wanniarachchi.

24. Sep. 2020

Date

JI YINLIN

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SUMMARY

Harvesting heat trapped in geothermal reservoirs has the potential to offer an affordable and sustainable solution for reducing the current dependence on fossil fuels. Hydraulic stimulation has been employed to enhance the permeability of geothermal reservoirs and to maintain the productivity of renewable heat. However, the effectiveness of heat extraction depends not only on the permeability of fractured granite but also on the injection-driven instability of pre-existing and hydraulically induced fractures. Over the past decades, induced seismicity resulted from fluid injection has sparked huge concerns for the safety and efficiency of deep geothermal systems. Therefore, understanding the mechanisms of injection-driven fracture instability is critical to the optimization of hydraulic stimulation strategies.

The objectives of this study are to uncover the mechanisms of injection-driven fracture instability and to develop new hydraulic stimulation strategies for mitigating injection-induced seismicity. The injection-driven shear tests are conducted on sawcut and natural fractures in granite using the triaxial shear-flow setup. The design of the triaxial shear-flow setup ensures that the frictional slip purely occurs along the fracture and the injected fluid solely flows through the fracture without interacting with the adjacent granite matrix. The jacket deformation and fracture area reduction of the triaxial shear-flow setup are also systematically corrected to accommodate the dynamic changes of the test system and sample assembly.

The experimental study reveals that the fracture surface heterogeneity and fluid pressure heterogeneity play significant role in controlling the injection-induced fracture instability. In the injection-driven shear tests, the sawcut and natural fractures exhibit dynamic slip (seismic) and quasi-dynamic sliding (aseismic), respectively, which are controlled by several physical processes, such as thermal pressurization and stress imbalance. The laboratory-derived fluid pressure heterogeneity is visualized in COMSOL simulations and increases with normal stress and injection rate, leading to the increase in fluid pressure at fracture failure. To explore the causal link between fluid pressure heterogeneity and fracture slip

propagation, the PFC2D modeling is developed and demonstrates that non-uniformly pressurized fractures exhibit global and localized drops of shear stress during the aseismic and seismic slip, respectively, and the slip transition dominates the fluid injection period. The fluid pressure front lags behind the slip front and shear stress transfer front during the aseismic slip. The results also suggest that the prediction of seismic moment release should consider not only injected volume but also fluid pressure distribution, especially for non-uniformly pressurized fractures. The optimization of hydraulic stimulation strategies is finally inspired by the in-depth understanding of injection-induced seismicity. Cyclic fluid injection promotes the fluid pressure diffusion on fractures, but the reduction in seismic moment release depends on several cycle-related factors, such as critical fluid pressure and injection frequency. Proper design of injection parameters is thus essential to balance the energy budget between seismic energy and hydraulic energy. This study also indicates that the activation of nearby blind, critically stressed faults in some circumstances may be inevitable and the resulted moment magnitude is mostly controlled by the tectonic stresses.

LIST OF PUBLICATIONS

All the publications derived from the work presented in this thesis are listed below:

- 1 Ji, Y., Wu, W. and Zhao, Z. (2019). "Unloading-Induced Rock Fracture Activation and Maximum Seismic Moment Prediction", Engineering Geology, 262: 105352.
- 2 Ji, Y. and Wu, W. (2020). "Injection-Driven Fracture Instability in Granite: Mechanism and Implications", Tectonophysics, 791: 228572.
- 3 Ji, Y., Wanniarachchi, W. A. M. and Wu, W. (2020). "Effect of Fluid Pressure Heterogeneity on Injection-induced Fracture Activation", Computers and Geotechnics, 123: 103589.

Conference papers and abstracts:

- 1 Ji, Y. and Wu, W. (2019). "Mitigating Risks of Injection-induced Seismicity using Cyclic Fluid Injection: New Evidence and Insights from the Laboratory", AGU Fall Meeting, San Francisco, USA.
- 2 Wu, W., Ji, Y. and Jia, Y. (2018). "Fracture Heterogeneity, Fluid Overpressure, and Injection-induced Seismicity", AGU Fall Meeting, Washington, D.C., USA.
- 3 Ji, Y. and Wu, W. (2018). "Overpressured Fluid Injection and Fault Slip Triggering", International Symposium on Energy Geotechnics, Lausanne, Switzerland.
- 4 Ji, Y. and Wu, W. (2018). "Overpressure-induced Frictional Slip of Rock Fractures", 10th Asian Rock Mechanics Symposium, Singapore.
- 5 Ji, Y., Jia, Y. Rathnaweera T. D. and Wu, W. (2018). "Overpressure-induced Fracture Slip Triggering in Granite", 2nd International Conference on Geo-mechanics, Geo-energy and Geo-resources, Chengdu, China.

Papers submitted:

- 1 Ji, Y., Yoon, J. S., Zang, A. and Wu, W., "Mitigation of Injection-induced Seismicity on Undrained Faults using Cyclic Fluid Injection", Journal of Geophysical Research: Solid Earth.

2 Ji, Y., Yoon, J. S., Zang, A. and Wu, W., "Shear Stress Transfer Induced by Fluid Overpressurization and Implications for Injection-induced Fault Activation", Geophysical Research Letters.

3 Ji, Y., Zhuang, L., Wu, W., Hofmann, H., Zang, A., and Zimmermann, G., "Activation of a Natural Fracture in Granite by Monotonic and Cyclic Water Injection", Rock Mechanics and Rock Engineering.

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LIST OF SYMBOLS

Symbol	Brief definition
a	scaling coefficient of direct velocity effect
A	fracture area
A_0	initial contact area of fracture
A_c	real contact area of fracture
A_{corr}	corrected contact area of fracture
A_p	cross-sectional area perpendicular to the flow direction in the parallel plate model
A_s	cross-sectional area of the sample
B	time-dependent increasing rate of real contact area of fracture
b	scaling coefficient of evolutionary velocity effect
b_g	gas slippage factor
c	cohesion
c_m	stress sensitivity coefficient
D_s	sample diameter
D	hydraulic diffusivity
D_c	characteristic distance in rate-and-state friction law
dl	corrected axial displacement
d, dl_s	slip/shear displacement along the fracture
e	hydraulic aperture
E_0	activation energy
E_h	hydraulic energy
E_r	seismic energy
F	total force from the pressurized fluid perpendicular to the fracture
G	shear modulus, or rigidity
h	reservoir thickness
H	material indentation hardness
K	stiffness of the apparatus-intact rock block system
k	permeability
k_0	intrinsic permeability at “zero” effective stress
k_∞	Klinkenberg-corrected permeability
k_a	apparent gas permeability
K_b	stiffness of rock blocks
K_f	fluid bulk modulus
k_c	rheological critical stiffness
k_{com}	combined stiffness of the test system and the fracture
K_m	stiffness of test system
k_{sample}	stiffnesses of the sample
k_{sur}	stiffness of the elastic surroundings
k_{system}	stiffness of the test system

l	length in x direction in the parallel plate model
l_c	chain length
L_s	length of the sample
M_0	seismic moment
$M_0(\text{max})$	maximum seismic moment
$M_0(\text{max-arr})$	maximum arrested moment
M_w	moment magnitude
p, P	fluid pressure
p_{down}	fluid pressure at the upstream side
p_{down}	fluid pressure at the downstream side
P_i	initial fluid pressure
P_{inj}	injection pressure
p_m	mean fluid pressure
P_{mon}	monitoring pressure
P_r	injection rate
Q	volumetric flow rate
q	sum of net flow volume
Q_{down}	the volumetric flow rate at the downstream side
r	fracture radius
r_r	radius of roller
R_i	initial circumference of sample
R_q	Root Mean Square (RMS) asperity height
S	area of sector $A_1A_2O_2$
S_p	area of the “ellipse-like” area bounded by the two arcs sharing a common chord A_1A_2
S_{Δ}	area of triangle $A_1A_2O_2$
t	elapsed time
T	temperature
V	slip velocity after an abrupt change
V_0	slip velocity at steady state
V_1	reference volume of the upstream
V_2	reference volume of the downstream
V_d	volume of pore space
V_{lp}	load point velocity
V_{max}	maximum voltage detected by the acoustic emission system
w	width in y direction in the parallel plate model
Z_i	sampling asperity height on the fracture surface
α	sensitivity of hydraulic aperture to the normal stress change
β	fracture inclination angle with respect to the horizontal plane
β_c	fluid compressibility

γ	reservoir parameter
γ_0	slope angle of Mohr-Coulomb envelope in terms of principal stresses
Δ	y-intercept of the deviator force versus load point displacement curve
ΔC	change in sample circumference
ΔF	deviator force
Δl	load point displacement
Δp	pressure difference across the sample
ΔV	injected volume
ΔV_d	pore volume change due to mechanical effect
$\Delta\mu_{ss}$	change of steady state friction coefficient after a slip velocity change
$\Delta\tau$	shear stress drop
ε_1	axial strain
ε_3	circumferential strain
ε_v	volumetric strain
θ	state variable
Θ	angle subtended by the two points of intersection of two overlapping circles, at the centres of the circles
θ_s	angle subtended by the chord length set by the extensometer
κ	bulk modulus of reservoir rocks
κ_B	Boltzmann constant
μ	friction coefficient
μ_0	friction coefficient at steady state
μ_1	friction coefficient of the greased fracture
μ_2	friction coefficient of the end-platen interface
μ_{aft}	friction coefficient after fluid lubrication
μ_{bef}	friction coefficient before fluid lubrication
μ_d	dynamic friction coefficient
μ_f	dynamic fluid viscosity
μ_i	internal friction coefficient
ρ	fluid density
σ_1	major principal stress
σ_3	minor principal stress
σ_c	uniaxial compressive strength
σ_{eff}	effective stress
ΣM_0	total moment release
σ_n	normal stress
σ_n'	effective normal stress
σ_{ncorr}	corrected normal stress
τ_{ncorr}	corrected shear stress

φ	angle of internal friction
χ	effective stress coefficient for permeability
ψ	fracture inclination angle with respect to the core axis
Ω	activation volume
f_b	body force per unit mass
τ	shear stress
∇P	pressure gradient along x direction in the parallel plate model
Σ	seismogenic index

CHAPTER 1 INTRODUCTION

1.1 Background

Crustal rocks are prone to fracturing processes in the ambient stress field, leading to fractures with different modes and lengths (Zoback, 2010). Human activities, such as fluid injection into underground formations for unconventional geo-energy exploitation and disposal of fracking wastewater, can also promote rock fracturing (Ellsworth, 2013). Currently, the injection of highly pressurized fluids into underground formations is indispensable in the unconventional geo-energy industry. Nevertheless in the past decades, injection-induced seismicity has been extensively reported worldwide in unconventional geo-energy systems, e.g., hydraulic fracturing in enhanced geothermal systems (Elsworth et al., 2016; Grigoli et al., 2018; Kim et al., 2018; Lee et al., 2019; Majer et al., 2007; Rathnaweera et al., 2020) and in shale gas reservoirs (Bao and Eaton, 2016; Lei, Huang, et al., 2017), as well as fracking wastewater disposal (Ellsworth, 2013; Kolawole et al., 2019; Walsh and Zoback, 2015). For instance, the 2017 M_w 5.5 earthquake that occurred in Pohang, South Korea is attributed to hydraulic fracturing in the nearby enhanced geothermal system (EGS) (Woo et al., 2019). The 2016 M_w 4.1 earthquake in Duvernay, western Canada results from hydraulic fracturing for shale gas extraction (Bao and Eaton, 2016). Several recent earthquakes (e.g., the 2016 M_w 5.8 Pawnee earthquake and the 2016 M_w 5.1 Fairview earthquake) in Oklahoma, United States are due to the disposal of fracking wastewater (Kolawole et al., 2019).

Recent advances show that the occurrence of injection-induced seismicity is strongly dependent on the shear-flow characteristics of fractures and faults (Elsworth et al., 2016). On the one hand, fluid injection can lower the shear strengths of fractures and faults by reducing the friction coefficient and effective normal stress. The friction coefficient decreases due to the lubrication effect (Brown and Ge, 2018; Cornelio et al., 2019; Diao and Espinosa-Marzal, 2018, 2019), and the reduction of effective normal stress is associated with fluid pressure diffusion. The injected fluid diffuses along fractures and faults and can also drive in-situ fluid into remote area due to the induced pressure gradient. The diffusion of fluid pressure is sensitive to fluid injection and diffusion rates (Cappa et al., 2018;

Passelegue et al., 2018; Rutter and Hackston, 2017). If the fluid pressure distributes heterogeneously on fractures and faults, the fluid pressure at failure can be underestimated by the Mohr-Coulomb failure criterion incorporating the effective stress law (Passelegue et al., 2018; Rutter and Hackston, 2017). These analyses and findings highlight the importance of flow characteristics of fractures and faults in controlling injection-induced seismicity.

On the other hand, the injection-induced frictional slip of fractures and faults may also be heterogeneous due to the spatial variation of frictional properties (Bhattacharya and Viesca, 2019; Cappa et al., 2019; Guglielmi, Cappa, et al., 2015). A marked similarity has been reported in several field cases that aseismic slip is first induced by localized fluid pressurization on the velocity-strengthening fault segment, and subsequently, seismic slip is triggered in the unpressurized fault segment characterized by velocity-weakening property due to the enhanced shear stress. Typical field examples include the field experiment injecting fluid into a fault cutting through a carbonate formation in France (Guglielmi, Cappa, et al., 2015), the 2016 M_w 4.1 earthquake resulting from hydraulic fracturing in Duvernay shale, western Canada (Bao and Eaton, 2016), and the 2016 M_w 5.8 Pawnee earthquake and the 2016 M_w 5.1 Fairview earthquake caused by wastewater disposal in Oklahoma, United States (Kolawole et al., 2019). These observations also cast doubt on the assumption that injection-induced earthquake is confined within the stimulated reservoir (McGarr, 2014; McGarr and Barbour, 2018), which may further invalidate the established relationships between the maximum magnitude of injection-induced earthquake and the stimulated reservoir volume (Shapiro et al., 2011), or the injected volume (Galis et al., 2017; McGarr, 2014). The injection-induced frictional characteristics of fractures and faults is thus also an important factor controlling the occurrence and magnitude of injection-induced earthquake. Therefore, understanding the shear-flow characteristics of fractures and faults is critical to uncovering the mechanism of injection-induced seismicity.

The jargon “fracture” represents the planar discontinuity within rock masses, along which no appreciable shear displacement has taken place, as opposed to the term

“fault” the planar discontinuity which has experienced an identifiable shear displacement, and thus is filled with gouge. A bare fracture in rocks may evolve to be a gouge-filled fracture by developing a finite amount of shear displacement, during which process brecciation is produced progressively by damaging fracture asperities, and sometimes adjacent rocks (Zoback, 2010). In laboratory studies, fractures and faults are commonly simulated by smooth sawcut fractures, rough artificial or natural fractures (Byerlee, 1967; Wu et al., 2017), and granular powders with various sizes and components (Marone and Scholz, 1989; Tembe et al., 2010). In addition, “activation” is normally used when describing “fracture” failure, while “reactivation” is normally used to describe “fault” failure because of the substantial preceding shear displacement. To maintain consistency, in the context of this thesis unless otherwise stated, “fracture” is used as a general term to describe the planar geological discontinuities having relatively low or zero tensile strengths. The term “fault” is only used when talking about large tectonic cases.

1.2 Research scope and objectives

This thesis aims to better understand the shear-flow characteristics of fractures in granite and provide implications for the mitigation of the seismic risks associated with fluid injection in deep geothermal systems. The objectives are summarized as below:

1. To evaluate the effect of fracture surface heterogeneity on the mode of injection-induced moment release.
2. To investigate the development of fluid pressure heterogeneity in a rock fracture and its influence on injection-induced fracture activation.
3. To reveal the effect of shear stress transfer induced by fluid overpressurization on injection-induced fracture activation.

4. To unravel the mechanism of seismic risk mitigation by cyclic fluid injection on locally undrained fractures and offer insights into cyclic fluid injection as a solution for seismic risk mitigation.

1.3 Organization of the thesis

This thesis consists of eight chapters, which commences with an overview of injection-induced seismicity in unconventional geo-energy industry (Chapter 1). The current understanding of this human-induced geohazard points out the importance of shear-flow characteristics of rock fractures in controlling the occurrence of injection-induced seismicity in deep geothermal systems.

Chapter 2 is intended to review the theoretical principles related to this study, including the Mohr-Coulomb failure criterion describing the fracture strength, the friction laws governing the fracture stability, the earthquake source parameters measuring the fracture slip, and the cubic law depicting the hydraulic properties of rock fractures. Along with these theories, the laboratory measurements of related parameters are introduced. A comprehensive review of previous work on shear-flow characteristics of rock fractures in the laboratory and field is also presented. Finally, some gaps in current research on injection-induced seismicity are identified.

The primary aim of Chapter 3 is to detail the facilities and methodology that have been employed in this study. The specifications of MTS 815 triaxial rock mechanics test system and VP-12K dual syringe pumps are first introduced, which is followed by the introduction of triaxial shear-flow setup, with emphasis on the experimental design and corrections. Acoustic emission signals during the triaxial shear-flow tests have been recorded using a system from the Physical Acoustic Corporation, so the basic principles and terminology of acoustic emission monitoring technique are introduced along with the system specifications. The David three-dimensional scanner has been used to quantify the surface roughness of fracture, and thus the scanner setup and the method of quantifying the roughness are also introduced.

In Chapter 4, triaxial shear-flow tests are carried out to study the mechanism of injection-induced instability of sawcut and natural fractures in granite. Particularly, the fractures approaching critical stress states are simulated before fluid injection. Fluid pressure heterogeneity, injection-induced fracture instability, acoustic emission characteristics and fracture surface evolution during the injection are carefully examined. The effects of fluid pressure heterogeneity and fracture surface heterogeneity are identified, and the mechanisms of injection-driven fracture instability (i.e., thermal pressurization and stress imbalance) are discussed in this chapter. The upper bounds of the maximum moment release predicted by McGarr (2014) and Galis et al. (2017) are also evaluated based on the test data.

Chapter 5 further deals with the effect of fluid pressure heterogeneity on injection-induced fracture activation by quantifying the pressurized area using COMSOL. Particularly, experimental and numerical methods are employed to observe the evolution of fluid pressure distribution on a rock fracture during the fluid injection and to reveal the relationship between the fluid pressure at fracture failure and the degree of fluid pressure heterogeneity.

Chapter 6 focuses on the effect of shear stress transfer induced by fluid overpressurization on injection-induced fracture activation. Laboratory and numerical simulations of injection-induced fracture activation are conducted, where the background shear stress is kept constant and the fluid is introduced locally. The relationship among fluid overpressurization, fracture slip propagation and shear stress transfer are explored by comparing the temporal evolution of fluid pressure front, slip front and shear stress transfer front. Finally, the results are validated by the field injection test in France, and some implications are drawn for injection-induced fracture activation in terms of maximum seismic moment prediction.

Chapter 7 aims to offer insights into cyclic fluid injection as a solution for seismic risk mitigation. A series of fluid injection tests is conducted on locally undrained fractures in low-permeability granite. The fluid pressure heterogeneity, injected volume, hydraulic energy, seismic moment, and seismic energy resulted from

monotonic and cyclic fluid injection conditions are analyzed and compared. This chapter highlights that properly designing the cycle-related injection parameters is essential to balance the energy budget between seismic energy and hydraulic energy for effective mitigation of seismic risks.

Chapter 8 concludes this thesis with discussion on the implications for injection-induced earthquakes. Some recommendations for future work are also outlined in this chapter.

In summary, the organization and structure of this thesis are shown in Figure 1-1.

Figure 1-1 Organization and structure of this thesis.

CHAPTER 2 REVIEW OF THEORY AND PREVIOUS WORK

2.1 Introduction

This chapter addresses basic theories related to the shear strength, frictional stability and hydraulic property rock fractures, including the Mohr-Coulomb failure criterion for fracture strength, friction laws for fracture stability, earthquake source parameters for fracture slip, and cubic law for fluid flow through rock fractures. Previous experimental works in the laboratory and field scales on shear-flow characteristics of rock fractures have also been comprehensively reviewed. Finally, research gaps are also pointed out at the end of this chapter. The organization and structure of this chapter are summarized in Figure 2-1.

Figure 2-1 Organization and structure of Chapter 2.

2.2 Mohr-Coulomb failure criterion

2.2.1 Basic formulation

With the aid of failure criterion, the failure stress of a material under various stress conditions can be predicted. The Mohr-Coulomb failure criterion describes the stress condition under which an isotropic material fails. It is generally acknowledged that both Charles-Augustin de Coulomb and Christian Otto Mohr have contributed to the establishment of this classic criterion (Labuz and Zang, 2012). The slope of the Mohr-Coulomb failure envelope normally reduces under higher confining pressure, while it is reasonable to assume a linear envelope for most rocks under small to moderate confining pressures (Zoback, 2010). This criterion has been extensively used in laboratory data analysis of rock mechanics due to its simplicity, although it neglects the influence of the immediate principal stress. It can be written as a relationship between the shear stress (τ) and normal stress (σ_n), and takes the following form,

$$\tau = c + \mu_i \sigma_n \quad (2-1)$$

where c is the cohesion (also referred to as the inherent shear strength), and μ_i is the internal friction coefficient,

$$\mu_i = \tan \varphi \quad (2-2)$$

where φ is the angle of internal friction.

For a plane inclined β to the horizontal plane (inset in Figure 2-2), the normal and shear stresses on this plane can be obtained by applying the stress transformation equations,

$$\sigma_n = \frac{1}{2}[(\sigma_1 + \sigma_3) + (\sigma_1 - \sigma_3) \cos 2\beta] \quad (2-3)$$

and

$$\tau = \frac{1}{2}(\sigma_1 - \sigma_3) \sin 2\beta \quad (2-4)$$

where σ_1 and σ_3 are the major and minor principal stresses, respectively.

Considering the critical state that the failure occurs purely along the plane, i.e. the envelope is tangent to the Mohr circle, the relationship between β and φ is,

$$\beta = \frac{\pi}{4} + \frac{\varphi}{2} \quad (2-5)$$

Figure 2-2 Illustrative diagram of Mohr-Coulomb failure envelope.

Substituting Eqs. (2-2)-(2-5) into Eq. (2-1), another form of Mohr-Coulomb failure criterion in terms of the principal stresses can be obtained,

$$\sigma_1 = \sigma_c + \sigma_3 \tan \gamma_0 \quad (2-6)$$

where

$$\sigma_c = \frac{2c \cos \varphi}{1 - \sin \varphi} \quad (2-7)$$

and

$$\tan \gamma_0 = \frac{1 + \sin \varphi}{1 - \sin \varphi} \quad (2-8)$$

where σ_c is the uniaxial compressive strength, γ_0 is the slope angle of Mohr-Coulomb envelope in terms of principal stresses. Eq. (2-6) can be expressed geometrically in Figure 2-3.

Figure 2-3 Mohr-Coulomb failure envelope in terms of principal stresses.

Although the Mohr-Coulomb failure criterion has been widely used, it has some intrinsic shortcomings (Brady and Brown, 2013; Labuz and Zang, 2012). For example, the failure pattern of intact rocks is not always shear mode, and even a shear fracture has been formed at the peak strength, the angle of the shear fracture is not always consistent with the predicted value. In addition, the Mohr-Coulomb failure criterion ignores the intermediate principal stress, which has a significant influence on the peak stress and failure pattern of rocks (Duan et al., 2016; Ma and Haimson, 2016).

2.2.2 Application to friction of rock fractures

Setting the cohesion term in Eq. (2-1) to zero, the Mohr-Coulomb failure criterion reduces to the well-known Amonton's law of friction (Lapusta and Barbot, 2012; Scholz, 2019),

$$\tau = \mu \sigma_n \quad (2-9)$$

where μ is the friction coefficient, τ and σ_n are shear and normal stresses resolved on the shear plane, respectively. As expressed in Eq. (2-9), Amonton's law of friction asserts a proportionality between the shear stress and normal stress on a plane, with the coefficient being referred to as the friction coefficient. Note that the friction coefficient (μ) in Eq. (2-9) is a material property describing the friction of a pre-

existing fracture, while the internal friction coefficient (μ_i) in Eq. (2-1) is a parameter that quantitatively describes how the strength of intact rocks increases with elevating confining pressure. It is also noted that the Amonton's friction law is not derived from the Mohr-Coulomb failure criterion. However, it is interesting that they share the same form. Although some studies show that the cohesion for a fracture may not be always zero (Scuderi et al., 2017), especially when clays are presented (Ikari and Kopf, 2011), it is still reasonable to take a zero cohesion for rock fractures due to the extremely low cohesion (Zoback, 2010). In most cases, the Mohr-Coulomb failure criterion and the Amonton's law of friction are used interchangeably. In this thesis, the Mohr-Coulomb failure criterion is used hereafter when talking about the fracture failure.

Figure 2-4 Application of Mohr-Coulomb failure criterion to various geo-energy reservoirs. μ_{bef} and μ_{aft} are the friction coefficients before and after the lubrication of injected fluid. c is nearly zero and negligible for rock fractures.

Considering the presence of pore pressure on a fracture, the Mohr-Coulomb failure criterion can be expressed as,

$$\tau = c + \mu(\sigma_n - p) \quad (2-10)$$

where $\sigma_n - p$ is the effective normal stress, p is the fluid pressure on the fracture. Eq. (2-10) gives the first impression that raising pore pressure tends to promote fracture

slip by reducing the effective normal stress and thus the shear strength. In fact, various changes of stress states and fluid pressures in geo-energy reservoirs can result in fracture slip according to Eq. (2-10) (Figure 2-4). These changes include the following:

1. Fluid pressure increases in the cases of hydraulic stimulation of unconventional geo-energy reservoirs, underground disposal of wastewater and carbon dioxide geosequestration, pushing the Mohr circle towards the failure envelope (Ellsworth, 2013; Elsworth et al., 2016).
2. The decrease of fluid pressure due to fluid extraction considering the poro-elasticity of reservoir rocks enlarges the Mohr circle to touch the failure envelope (Zbinden et al., 2017).
3. The friction coefficient of fracture can be reduced due to the lubrication effect of the injected fluid (Brown and Ge, 2018; Diao and Espinosa-Marzal, 2018, 2019), causing the failure envelope to be more approachable.

2.3 Friction laws and fracture stability

2.3.1 From slip-weakening law to rate-and-state friction law

Brace and Byerlee (1966) first proposed that the laboratory observed stick-slip instability during sliding along rock fracture might be analogous to that happened along tectonic faults during earthquakes. Since then, extensive studies have been carried out to investigate the properties of rock friction, and the conditions under which the stick-slip instability may occur. During the displacement-driven shear test, rock fractures exhibit two typical modes of slip (i.e., stable sliding and unstable slip) depending on the frictional characteristics of rock fractures and the stiffness of surrounding rocks (Marone, 1998b; Rice, 1983; Scholz, 1998). Two friction laws have been proposed to depict the stability of a fracture. In the classic slip-weakening friction law, the friction of a fracture degrades linearly to a lower value through a certain amount of shear displacement. A slip-weakening fracture shows stable sliding in a stiffer loading system, while softer loading system favors

unstable slip. For a single fracture slip sequence, the slip-weakening friction law has been widely used to describe the unstable slip motion of rock fractures (Ampuero et al., 2002; Lapusta and Barbot, 2012; Rice, 1983). However, this model is not explicitly dependent on slip velocity and fails to consider the fracture restrengthening after a slip motion. A more comprehensive friction law is the rate-and-state friction law, which can remedy the limitations of the slip-weakening friction law and can describe repeated slip motions (Dieterich, 1979; Ruina, 1983; Tse and Rice, 1986). Dieterich (1972) was the first who discovered the time-dependency of rock friction. This property depicts that the friction coefficient increases with the logarithm of the time of stationary contact. Another essential characteristic of rock friction is its velocity-dependency, which was first reported by Scholz and Engelder (1976) from slips of corundum on Westerly granite, on Solenhofem limestone, and on Twin Sister dunite. Specifically, the friction coefficient is inversely proportional to the logarithm of slip velocity. Moreover, Dieterich (1979) noticed there is an instantaneous velocity effect on the friction coefficient. That is, there is a positive jump of friction coefficient, when the slip velocity is suddenly increased, and a negative jump when the slip velocity is suddenly decreased. Dieterich (1978; 1979) had attempted to mathematically describe the static and sliding friction along rock surface. Assuming that the shear strain is homogeneous, the evolution of friction coefficient with displacement can be characterized by shear strain. Nevertheless, Dieterich (1981) found that the deformation tends to localize rather than always homogenous from the tests on simulated gouge. Additionally, the slip process may couple with various factors, such as, chemical diffusion, heat conduction, and/or pore pressure diffusion, which makes the evolution of friction coefficient more complicated (Ruina, 1983). As such, Ruina (1983) generalized these surrounding influences into state variables to represent the surface state. The evolution of friction coefficient after an abrupt change of slip velocity can be a function of the state variables. The rate-and-state friction law (Figure 2-5) that contains one state variable is the most popular in use (Marone, 1998b; Scholz, 1998):

$$\mu = \mu_0 + a \ln\left(\frac{V}{V_0}\right) + b \ln\left(\frac{V_0 \theta}{D_c}\right) \quad (2-11)$$

where μ_0 is the friction coefficient at steady state when the slip velocity is V_0 , $a \ln(V/V_0)$ is the direct velocity effect, and $b \ln(V_0\theta/D_c)$ as the evolutional effect characterizes the evolution of friction coefficient following an abrupt change in slip velocity from V_0 to V , a and b are the corresponding scaling coefficient, and D_c is the characteristic distance over which the contact population has been renewed. Besides, the state variable (θ) evolves according to either of the following two equations:

$$\frac{d\theta}{dt} = 1 - \frac{V\theta}{D_c} \text{ (ageing law)} \quad (2-12)$$

$$\frac{d\theta}{dt} = -\frac{V\theta}{D_c} \ln\left(\frac{V\theta}{D_c}\right) \text{ (slip law)} \quad (2-13)$$

Dieterich law considers rock friction from the aspects of time-dependence and static friction, so it is also referred to as ageing law. For this reason, it is able to represent the friction coefficient evolution under stationary condition (Beeler et al., 1994; Marone, 1998b). Ruina law is established based on the positive view that any change in friction results from velocity change, and that is why it is also called slip law. Therefore, it better models the state evolution due to large jump of slip velocity (Ruina, 1983). Despite the different views on friction evolution, these two laws can reproduce the laboratory results very well and in similar modes (Marone, 1998b).

At steady state (Marone, 1998b; Scholz, 1998),

$$\frac{d\theta}{dt} = 1 - \frac{V\theta}{D_c} = -\frac{V\theta}{D_c} \ln\left(\frac{V\theta}{D_c}\right) = 0 \quad (2-14)$$

$$\theta = \frac{D_c}{V} \quad (2-15)$$

$$(a-b) = \frac{\Delta\mu_{ss}}{\ln(V/V_0)} \quad (2-16)$$

where $\Delta\mu_{ss}$ is the change of steady state friction coefficient after a slip velocity change, $a-b$ is the friction rate parameter.

Figure 2-5 The response of friction coefficient to slip velocity changes described in the rate-and-state friction law.

For a positive friction rate parameter (i.e., $a-b>0$), the friction coefficient increases as the slip rate increases. This sliding is velocity strengthening and thus it is always stable. When the friction rate parameter is negative (i.e., $a-b<0$), the friction coefficient decreases following an increase in slip rate. The sliding is velocity weakening and it is conditionally unstable. Particularly, the sliding tends to be unstable when $k_{sur} < k_c = (b-a)\sigma_n / D_c$, where k_{sur} is the stiffness of the elastic surroundings, k_c is the rheological critical stiffness, and σ_n is the normal stress (Dieterich, 1979; Ruina, 1983; Scholz, 1998; Scuderi et al., 2016). Instability occurs due to force imbalance and acceleration when $k_{sur} < k_c$, because the rate of elastic unloading is exceeded by the fracture weakening rate. Otherwise, the slip is stable for a system with higher stiffness, where the fracture weakening rate is less than the elastic unloading rate.

When the injection-driven frictional stability is evaluated in the context of friction laws, the increase in fluid pressure reduces the effective normal stress and thus the critical stiffness of the fracture, favoring stable sliding (aseismic) instead of unstable slip (seismic). However, experimental results demonstrate that the increase of fluid pressure can change the friction rate parameter ($a-b$) from positive to

negative, promoting unstable slip (Scuderi and Collettini, 2016). This contradiction suggests the poor understanding on the underlying mechanisms of injection-induced seismicity.

2.3.2 Laboratory measurement and interpretation

In the laboratory experiments, the stiffness of the experimental apparatus is not infinite, so the frictional behavior of sliding surface is controlled by the mechanical interaction between the sliding surface and experimental apparatus. To obtain the intrinsic frictional properties of the sliding surface, the contribution of the experimental apparatus should be considered.

For the slip-weakening law, the slip-weakening rate of fracture is obtained by dividing the difference between static friction coefficient and dynamic friction coefficient by the evolving displacement. The combined stiffness (k_{com}) of the test system and the fracture is mathematically calculated from the stiffnesses of the sample (k_{sample}) and test system (k_{system}) as,

$$\frac{1}{k_{com}} = \frac{1}{k_{sample}} + \frac{1}{k_{system}} \quad (2-17)$$

It can be estimated as the slope of the linear portion of the friction coefficient versus load point displacement curve, because the fracture can be assumed to be locked (i.e., $1/k_{sample}=0$) in the linear elastic loading period and nearly all the deformation is from the loading system (McLaskey and Yamashita, 2017). This method for estimating the stiffness of loading system is extensively employed in both direct shear tests (Karner and Marone, 2000; Leeman et al., 2018) and triaxial shear tests (McLaskey and Lockner, 2014).

For the rate-and-state friction law, the elastic interaction between the sliding surface and experimental apparatus can be described as follows (Marone, 1998b),

$$\frac{d\mu}{dt} = K(V_{lp} - V) \quad (2-18)$$

where K is the stiffness of the apparatus-intact rock block system under constant normal stress, and V_{lp} is the load point velocity. In each test, K is the slope of the linear portion of friction coefficient versus loading point displacement curve under the corresponding normal stress (Kohli and Zoback, 2013; Tembe et al., 2010). The Runge-Kutta numerical method with adaptive step size has been normally used to solve Eq (2-12) or Eq. (2-13) and Eq. (2-18) with Eq. (2-11) as the constraint. An iterative least squares method then determines the best fit values of a , b , and D_c (Reinen and Weeks, 1993; Saffer and Marone, 2003; Tinti et al., 2016).

The interpretation of slip-weakening law is straightforward. Nevertheless, the laboratory-derived rate-and-state friction law is empirical, and thus extensive studies have been devoted to providing physical interpretations of the rate-and-state parameters. The physical processes controlling the friction of rock fractures are complicated, which include contact creep, subcritical cracking, granular flow, grain boundary sliding, and shear-induced dilation/compaction (Chen, Niemeijer, et al., 2017). After reaching a steady state friction, microcracking, grain comminution and rolling, are not important deformation mechanisms. Although granular flow in the localized shear band, which leads to shear-induced dilation/compaction, still exist in friction evolution (Chen, Niemeijer, et al., 2017; Chen and Spiers, 2016), most studies based purely on the contact creep mechanism can capture the steady state friction very well (Aharonov and Scholz, 2017; Ikari et al., 2016; Molinari and Perfettini, 2017; Perfettini and Molinari, 2017). This is presumably because of the small impact of shear band due to its extremely low fraction in the whole shear zone. The basic idea of contact creep is that a much higher normal stress and shear stress on real contacts lead to plastic deformation (Bowden and Tabor, 1964). It is acknowledged that the shear yielding of microscopic contacts is the underlying origin of macroscopic friction phenomenon.

Based on the theory of contact creep, Ikari et al. (2016) derived the evolution of friction coefficient assuming the local normal contact stress equals the material hardness under constant normal stress as,

$$\mu = \frac{E_0}{H\Omega} + \frac{\kappa_B T}{H\Omega} \ln\left(\frac{V}{V_0}\right) + \frac{E_0 B}{2.3H\Omega A_c} \ln\left(\frac{V_0 \theta}{D_c}\right) + \frac{\kappa_B T B}{2.3H\Omega A_c} \ln\left(\frac{V}{V_0}\right) \ln\left(\frac{V_0 \theta}{D_c}\right) \quad (2-19)$$

Comparing this equation with the rate-and-state friction expression, the following equations can be obtained,

$$\mu_0 = \frac{E_0}{H\Omega} \quad (2-20)$$

$$a = \frac{\kappa_B T}{H\Omega} \quad (2-21)$$

and

$$b = \frac{E_0 B}{2.3H\Omega A_c} + \frac{\kappa_B T B}{2.3H\Omega A_c} \ln\left(\frac{V}{V_0}\right) \quad (2-22)$$

where E_0 is the activation energy, H is the material indentation hardness, Ω is the activation volume, κ_B is the Boltzmann constant, T is the temperature, A_c is the real contact area, and B is the time-dependent increasing rate of A_c .

Because the negligible contribution to b , the second term in Eq. (2-22) can be neglected. Substituting the expression for base friction coefficient (Eq. (2-20)) into the equations of a and b , the following equations can be obtained,

$$a = \frac{\mu_0 \kappa_B T}{E_0} \quad (2-23)$$

$$b = \frac{\mu_0 B}{2.3A_c} \quad (2-24)$$

To evaluate the slip stability in the framework of the rate-and-state friction law, b/a can also be used for the convenience of mathematical expression,

$$\frac{b}{a} = \frac{E_0 B}{2.3\kappa_B T A_c} \quad (2-25)$$

2.4 Earthquake source parameters and fracture slip

The unstable fracture slip caused by shear rupture propagating along the fracture can lead to sudden motions and generate seismic waves, which comprises the most

common type of earthquakes. The source parameters of earthquakes (e.g., seismic moment, moment magnitude, stress drop and slip displacement) have been extensively used to describe fracture slip (Scholz, 2019), which are thus reviewed in this section.

2.4.1 Seismic moment and moment magnitude

The seismic moment (M_0) is defined as the product of shear modulus (or rigidity) (G), fracture area (A), average slip displacement (d) as (Aki, 1966),

$$M_0 = GAd \quad (2-26)$$

The magnitude of an earthquake is routinely reported using the moment magnitude (M_w), which is estimated from the seismic moment as (Hanks and Kanamori, 1979),

$$M_w = \frac{\log M_0}{1.5} - 6.07 \quad (2-27)$$

In addition, according to a statistical analysis of historical earthquakes worldwide, the moment magnitude of earthquake is a function of the rupture area (A) with a standard deviation of 0.24 (Wells and Coppersmith, 1994),

$$M_w = 4.07 + 0.98 \log(A) \quad (2-28)$$

2.4.2 Scaling of stress drop and slip displacement

The stress drop ($\Delta\tau$) on a circular fracture with a radius of r during an earthquake of a seismic moment of M_0 can be expressed as (Stein and Wyssession, 2009; Zoback and Gorelick, 2012),

$$\Delta\tau = \frac{7}{16} \frac{M_0}{r^3} \quad (2-29)$$

The slip displacement (d) during an earthquake is also a function of M_0 and r (Stein and Wyssession, 2009; Zoback and Gorelick, 2012),

$$d = \frac{7}{8} \frac{M_0}{Gr^3} \quad (2-30)$$

(a)

(b)

Figure 2-6 Earthquake magnitude and source parameters. (a) Earthquake magnitude as a function of rupture area (after Wells and Coppersmith, 1994). (b) Relationships among various scaling parameters for earthquakes (after Zoback and Gorelick, 2012).

It is suggested by Eqs. (2-28)-(2-30) that large earthquakes can only be produced on large fracture with a considerable amount of slip (Figure 2-6). The magnitude of an earthquake generally increases with rupture area, slip displacement and stress drop.

2.4.3 Laboratory measurement and interpretation

In the laboratory experiment, the fracture area, slip displacement and shear stress drop can be easily determined for a fracture slip event. Normally, the entire laboratory-scale fracture ruptures and accelerates quasi-uniformly in a fracture slip event that is fueled by the compliance of the test system (Rice, 1983). That is, the fracture area and rupture area are equal for the small laboratory scale (McLaskey and Lockner, 2014). The key to estimating the moment release resulted from fracture slip using Eq. (2-26) is to estimate the modulus of rigidity, also known as Lamé's second parameter. Previous studies use the rigidity of the testing material directly when estimating the seismic moment using Eq. (2-26) (McGarr, 2012; McGarr and Barbour, 2018; McGarr et al., 2010). It is true in the field that the seismic moment resulted from fracture instability is mainly dependent on the rigidity of host rocks. However, the fracture instability test in the laboratory experiment is strongly machine dependent (Dieterich, 1979). Therefore, a rigorous estimate of the combined rigidity of the test system and the fracture is a necessity for a more reasonable estimate of the moment release during fracture instability in laboratory experiments.

2.5 Hydraulic properties of rock fractures

2.5.1 Cubic law for fluid flow

To better understand the classic cubic law for the fluid flow through rock fractures, the detailed derivation of cubic law is presented here.

The Navier-Stokes equations derived from the principle of linear momentum conservation (i.e. Newton's second law of motion) are used to describe the incompressible flow of a Newtonian viscous fluid (Batchelor, 2000):

$$\frac{D\vec{V}}{Dt} = \frac{\partial \vec{V}}{\partial t} + (\vec{V} \cdot \nabla) \vec{V} = \vec{f}_b - \frac{1}{\rho} \nabla P + \frac{\mu_f}{\rho} \nabla^2 \vec{V} + \frac{1}{3} \frac{\mu_f}{\rho} \nabla (\nabla \cdot \vec{V}) \quad (2-31)$$

where $\frac{D\vec{V}}{Dt}$ is the material derivative of velocity vector $\vec{V}$, and is defined as

$$\frac{\partial \vec{V}}{\partial t} + (\vec{V} \cdot \nabla) \vec{V}, \quad \vec{f}_b \text{ is the body force vector per unit mass, } \rho \text{ is the fluid density,}$$

∇P is the pressure gradient, and μ_f is the dynamic fluid viscosity.

For steady flow, defining $P = p + \rho g z$, which assumes that the fluid gravity is the only applied body force and z direction upwards, the Navier-Stokes equations reduce to (Zimmerman and Bodvarsson, 1996)

$$\frac{\mu_f}{\rho} \nabla^2 \vec{V} - (\vec{V} \cdot \nabla) \vec{V} = \frac{1}{\rho} \nabla P \quad (2-32)$$

The advective acceleration term in Eulerian coordinates is nonlinear, and therefore the equations are extremely difficult to be solved. More assumptions should be made to further simplify the equations, and consequently a parallel plate model with smooth surface was proposed (Figure 2-7). The advective acceleration term will be identically equal to zero, and thus the Navier-Stokes equations can be further reduced to the steady-state Stokes equations, which can be exactly solved in this case (Zimmerman and Yeo, 2000),

$$\mu_f \nabla^2 \vec{V} = \nabla P \quad (2-33)$$

Figure 2-7 Parallel plate model with smooth fracture surface.

For the parallel plate model, a Cartesian coordinate system is defined, where the x direction is parallel to ∇P , y is perpendicular to x in the plane of the fracture, and z is perpendicular to the plane of the fracture (Figure 2-7).

Because the velocity components do not vary in the y and z directions, so:

$$\nabla^2 \vec{V}_x(z) = \nabla P / \mu_f \quad (2-34)$$

$$\nabla^2 \vec{V}_y(z) = \nabla P / \mu_f \equiv 0 \quad (2-35)$$

$$\nabla^2 \vec{V}_z(z) = \nabla P / \mu_f \equiv 0 \quad (2-36)$$

According to the boundary conditions, i.e., $\vec{V} \equiv 0$ when $z = \pm e/2$, the velocity function can be obtained by integrating $\nabla^2 \vec{V}_x(z) = \nabla P / \mu_f$ twice with respect to z ,

$$\vec{V}_x(z) = \frac{\nabla P}{2\mu_f} [z^2 - (e/2)^2] \quad (2-37)$$

The total volumetric input to the parallel plate fracture with a width of w can be obtained by performing integration of the velocity across the fracture in the range $(-e/2, e/2)$,

$$Q = w \int_{-e/2}^{e/2} \vec{V} \cdot dz = w \int_{-e/2}^{e/2} \frac{\nabla P}{12\mu_f} [z^2 - (e/2)^2] \cdot dz = \frac{-\nabla P w e^3}{12\mu_f} \quad (2-38)$$

The above equation is the classic cubic law.

With a further assumption that the linear Darcy's law is still valid, i.e., $Q = -k A_p \nabla P / \mu_f$, where $A_p = we$, the permeability can be calculated as,

$$k = \frac{e^2}{12} \quad (2-39)$$

Nevertheless, the original cubic law is derived based on the assumption that the fluid flows through the planar fractures bounded by two smooth walls, which do not really exist at in-situ geological fields. To account for the more realistic conditions

(i.e., roughness of fracture walls and varied apertures), extensive efforts have been devoted to extending the validity of cubic law by revising the expression considering the wall roughness (Lomize, 1951), and asperity contact area (Cammarata et al., 2007; Cook et al., 1990; Neuzil and Tracy, 1981).

2.5.2 Laboratory measurement of permeability

In the laboratory, high permeability can be measured by steady-state flow technique (Scheidegger, 1958), and low permeability by pulse-decay technique (Brace et al., 1968). The two techniques are both based on the validity of one-dimensional Darcy's law. The penetrating fluid can be refined mineral oil, distilled water, or some types of inertia gases such as argon.

The steady-state flow technique records the volumetric flow rate of water through the sample under a constant pore pressure difference between two ends. For permeability measured by liquid fluids such as water, it can be directly calculated as,

$$k = \frac{Q \cdot L_s \cdot \mu_f}{A_s \cdot \Delta p} \quad (2-40)$$

where Q is the volumetric flow rate, μ_f is the dynamic fluid viscosity, A_s is the cross-sectional area of the sample, and Δp is the pressure difference across the sample of length (L_s).

However, for gaseous fluids such as argon, changing volumetric flow rate along the sample due to gas expansion should be considered. The differential form of Darcy's law should be used,

$$Q = \frac{k_a A_s}{\mu_f} \frac{dp}{dL_s} \quad (2-41)$$

where k_a is the apparent gas permeability.

According to the principle of mass conservation and Boyle's law,

$$Q = Q_{down} p_{down} / p \quad (2-42)$$

where Q_{down} and p_{down} are the volumetric flow rate and pressure at the downstream side, respectively.

Substituting this equation into the differential form of Darcy's law (Eq. (2-41)), the following can be obtained,

$$k_a = \frac{Q_{down} p_{down} \mu_f}{A_s} \frac{dL_s}{p dp} \quad (2-43)$$

After integrating, the average gas permeability is,

$$k_a = \frac{2Q_{down} p_{down} \mu_f L_s}{A_s (p_{up}^2 - p_{down}^2)} \quad (2-44)$$

For the pulse-decay technique, it records the decay of the differential pressure after imposing a pressure step at the two ends of the sample, and the water permeability is calculated as (Brace et al., 1968)

$$k = \frac{\mu_f \beta_c L_s (\ln(\frac{\Delta P_i}{\Delta P_f}) / \Delta t)}{(\frac{1}{V_1} + \frac{1}{V_2}) A_s} \quad (2-45)$$

where β_c is the fluid compressibility, V_1 and V_2 are the reference volumes in the up- and down- streams respectively, L_s is the sample length, and $\ln(\frac{\Delta P_i}{\Delta P_f}) / \Delta t$ is the slope of the fitted line for the natural log of differential pressure.

Likewise, for the cases where a single-phase gas has been used as the fluid, the following calculation is derived considering gas expansion along the sample length,

$$k_a = \frac{\mu_f L_s (\ln(\frac{\Delta P_i}{\Delta P_f}) / \Delta t)}{(\frac{1}{V_1} + \frac{1}{V_2}) A_s p_m} \quad (2-46)$$

where p_m is the mean fluid pressure.

Note that the permeability measured by water is referred to as intrinsic water permeability, while the permeability measured by gas is the apparent gas permeability, which is a function of mean pore pressure because of the Klinkenberg phenomenon. This phenomenon refers to the non-Darcy flow of gas in porous media, the effect of which should be subtracted from the measured value. The

correction on the measured value can be implemented using the Klinkenberg equation (Ghanizadeh et al., 2014),

$$k_a = k_\infty (1 + b_g \cdot \frac{1}{p_m}) \quad (2-47)$$

where k_∞ is the Klinkenberg-corrected permeability, and b_g is the gas slippage factor.

Particularly, for the measurement of fracture permeability, water is normally used as the permeating fluid. However, the cross-sectional area of the fracture perpendicular to the flow direction is unknown because the hydraulic aperture has yet to be determined. Therefore, Eq. (2-40) cannot be used to calculate the fracture permeability. Instead, the cubic law can be used to first calculate the hydraulic aperture,

$$e = (\frac{12Q\mu_f}{\nabla P_w})^{1/3} \quad (2-48)$$

Finally, the fracture permeability can be estimated from the hydraulic aperture by Eq. (2-39).

2.6 Previous shear-flow tests on rock fractures

The shear-flow characteristics of rock fractures have long been of great interest to researchers in rock mechanics and geophysics. Especially in recent years, increasing efforts have been devoted to better understand the frictional stability and hydraulic property of fractures due to growing attention on injection-induced seismicity. A comprehensive coverage of the literature on this topic has been given in this section, which is divided into five parts, including shear tests, flow tests, displacement-driven shear-flow tests, and fluid-driven shear-flow tests on rock fractures, as well as field fluid injection tests on in-situ fractures.

2.6.1 Shear tests on rock fractures

Byerlee (1978) proposed a classic friction law for the frictional strength of rock material that is later referred to as the Byerlee's friction law. The law states that the shear strength of rock fractures increases linearly with the normal stress ($\tau=0.85\sigma_n$)

when the normal stress is below ~200 MPa. The shear strength is also dependent on the rock type and fracture surface roughness in this lower normal stress regime. While above ~200 MPa normal stress, the shear strength still increases with increasing normal stress ($\tau=0.5+0.6\sigma_n$), but the increment rate declines and tends to be independent of the rock type and fracture surface roughness (Figure 2-8). That is, the Byerlee's friction law generalizes the friction coefficient of rock fractures to a value between 0.6 and 0.85.

Figure 2-8 Maximum shear stress as a function of normal stress for various rocks at normal stress up to ~200 MPa (after Byerlee, 1978).

Some early attempts have been made to understand the basic mechanisms and characteristics of rock friction. Byerlee (1967) measured the frictional characteristics of fractures in granite using the triaxial shear setup. The friction coefficient is found to first increase to the maximum value and then decrease to a constant lower value, which is primarily attributed to the rolling of wear particles. Thus, the brittle failure of asperities is considered as the main physical process controlling the frictional sliding of brittle rocks. Byerlee (1967) also showed that water reduces the friction coefficient of fractures in granite independent of the normal stress, which is the well-known lubrication effect of water on frictional strength of rock fractures. Also important in this paper is the proposal of stick-slip

event along pre-existing fracture as an analogue of crustal earthquakes (Brace and Byerlee, 1966).

There are a great many internal and external factors probably affecting the frictional characteristics of rock fractures, including the fracture surface roughness, mineral composition of rock material, applied normal stress and slip velocity, as well as temperature and powdered gouge generated during shear. Rougher rock fractures are normally characterized by higher frictional strength and more positive velocity strengthening behavior due to the cohesive interlocking effect of larger asperities (Fang, Elsworth, Ishibashi, et al., 2018). The subsequent reduction in frictional strength and transition to velocity weakening behavior is attributed to the reduction in the cohesive interlocking effect caused by shear-induced asperity damage. The compliant mineral compositions of rock material may also influence the frictional behavior of rock fractures. Kohli and Zoback (2013) carried out a comprehensive study to investigate the clay and organic content on the frictional behavior of shale reservoir rocks using samples from three hydrocarbon reservoirs (i.e., the Barnett, Haynesville, and Eagleford shales in the United States). The transition from velocity-weakening to velocity-strengthening occurs with increasing clay and organic content at ~30 wt%. The evolution of velocity dependence characteristics implies a change in the grain-packing framework from rigid clast supported to clay and organic supported. Similar studies investigating the mineralogical control on frictional stability of rock fractures include Fang et al. (2017), Fang, Elsworth, Wang, et al. (2018) and Zhang, An, et al. (2019). Kilgore et al. (1993) studied the velocity-dependent friction of bare Westerly granite surface over a wide range of normal stresses (i.e., 5, 15, 30, 70, and 150 MPa), and slip velocities (i.e., 10^{-4} - 10^3 $\mu\text{m/s}$). At velocities between 10^{-4} -1 $\mu\text{m/s}$, consistent velocity weakening behavior is observed, with more negative dependence at lower velocities. When the velocity is larger than 10 $\mu\text{m/s}$, velocity weakening (under 30, 70, and 150 MPa), velocity neutral (under 15 MPa), and velocity strengthening (under 5MPa) are observed under different normal stresses. These observations suggest that stable sliding along fractures in granite is favored at shallow depths. Blanpied et al. (1998) examined the influences of shear displacement, slip velocity, and temperature (induced by

heating) on the velocity-dependent friction of bare Westerly granite surface. The results indicate that the fracture in granite is intrinsically velocity weakening and temperature strengthening. The velocity dependence of fractures in granite transforms to velocity strengthening at high slip rates because the temperature-strengthening effect dominates the competition between velocity weakening and temperature strengthening. However, at much faster slip rates (>10 cm/s), flash heating temperature (~ 1000 °C) can be reached to melt the asperities and thus weaken the fracture (Acosta et al., 2018; Aubry et al., 2018; Rice, 2006). In addition, the lubrication effect of the powdered gouge generated during fast fracture slip approaching earthquake slip rate can also be a common and effective mechanism for the dynamic fracture weakening process (Reches and Lockner, 2010).

2.6.2 Flow tests on rock fractures

The cubic law has been extensively used to describe the linear fluid flow through a single rock fracture. However, the cubic law is only applicable to laminar flow through relatively smooth parallel plates, in which the fracture permeability solely depends on the hydraulic aperture.

On the one hand, rock fractures are not idealized smooth parallel plates. Rather, they have different roughness and varying tortuous fluid pathways under changing stress states. Therefore, many studies have been carried out to address this intrinsic problem of the classic cubic law for fluid flow through fractures. For example, Zhang and Nemcik (2012) proposed a friction factor for fluid flow considering fracture wall friction and flow pathway tortuosity to correct the classic cubic law, which has achieved a high precision on fluid flow prediction. Chen, Liang, et al. (2017) established a mathematical model considering the fracture geometric characteristics to depict the hydraulic conductivity of rough-walled fracture, focusing on the effects of the aperture size of interconnected void space, the contact areas of fractures and the heterogeneous distribution of interconnected void space. Vogler et al. (2018) investigated the effect of fracture surface heterogeneity on fluid flow during fracture closure. Using a finite element method-based simulation framework, they proved that the measured heterogeneous aperture distribution in

the laboratory can be used as input parameter to reproduce the test results based on a local cubic law model for a heterogenous fracture.

On the other hand, the transition from linear to nonlinear flow regimes through rock fractures is also of great interest. Zhang and Nemcik (2013) determined the fluid flow regimes of mated and non-mated rock fractures, and they found that under the same stress condition, non-mated fractures are more prone to nonlinear flow due to the large apertures than the mated ones. Moreover, higher normal stress induces fracture closure and thus favors non-linear flow (Zhang et al., 2014). However, rock fractures in the field scale normally distribute randomly in rock masses and connect with each other, forming discrete fracture networks. Liu et al. (2016) investigated the hydraulic properties of a rock fracture network, focusing on the effects of aperture, fracture surface roughness, and number of intersections on the critical hydraulic gradient for linear to nonlinear fluid flow transition. They found that larger apertures, rougher fracture surfaces, and more intersections in rock fracture networks favor a lower critical hydraulic gradient.

Additionally, temperature also has a profound influence on fracture permeability. Polak et al. (2003) experimentally studied the permeability evolution of a natural fracture in Arkansas novaculite under temperatures ranging from 20 ° to 150 °, focusing on the influence of mineral dissolution under hydrothermal conditions. They found that the reduction of aperture is consistent with the dissolution of stressed apertures in contact. Small temperature change can exert a large influence on aperture change and thus the fracture permeability. Yasuhara et al. (2004) simulated this test using a mechanistic model and found that the fracture closure rate scales exponentially with temperature.

2.6.3 Displacement-driven shear-flow tests on rock fractures

As a conventional experimental method, displacement-driven shearing of rock fractures has been extensively employed in the investigation of shear-flow characteristics of rock fractures.

There is a considerable body of experimental work examining the evolution of fracture permeability during displacement-driven shearing of rock fractures. Fractures have a profound effect on fluid transport. Gutierrez et al. (2000) found that the permeability of de-mineralized fractures in shale either increases after shearing under low effective stress or decreases under high normal stress. This finding was later proved by an experimental study reported by Wu et al. (2017). These two studies also showed the permeability of fractures is always larger than shale matrix, indicating that the fracture serves as a major fluid pathway once being created. The mineral composition, stress states and slip history are important factors controlling the fracture permeability evolution during shearing. Kaproth et al. (2016) conducted a series of shear-flow tests on halite-clay-quartz gouge to examine the effects of these three factors on permeability evolution. They found that normal stress and great shear strain (larger than ~ 5) can barely affect the fracture permeability, while clay content has the greatest influence on fracture permeability. More recently, Rutter and Mecklenburgh (2018) found that the increases in shear stress accelerated the reduction in permeability due to the blockage of pathways caused by wear particles. The permeability evolution during shearing of gouge can also be different in different directions. Okazaki et al. (2013) analyzed the permeability anisotropy developed during shear slip of antigorite serpentinite gouge. The initial permeabilities were similar in all directions, while permeability anisotropy develops with increasing shear displacement until the shear stress reaches the steady state, with the permeability normal to the shear plane being lower than the other two orthogonal directions. This anisotropy is related to the formation of shear localization and can enhance fluid flow parallel to the shear direction.

The presence of pore fluid may alter the friction rate parameters of rock fractures. Samuelson and Spiers (2012) demonstrated that the addition of either brine or supercritical carbon dioxide has no obvious influence on frictional stability of North Sea reservoir sandstones and caprocks by conducting laboratory velocity-stepping tests. The results show that the rocks are velocity strengthening, suggesting that fractures in these rocks should slip aseismically, regardless of the presence of supercritical carbon dioxide. Scuderi and Collettini (2016) showed the evolution of

rate-and-state parameters of carbonate gouge with increasing fluid pressure from sub-hydrostatic to near lithostatic fluid pressure. The results show the friction rate parameter ($a-b$) changes from velocity strengthening to velocity-neutral behavior, and the critical slip distance D_c decreased with the increasing fluid pressure. They attribute the decrease of ($a-b$) to layer dilation and decreasing D_c to reduced contact area due to high fluid pressure. Elevated ambient temperature can also influence the frictional property of fractures with the presence of pore fluid. For example, Blanpied et al. (1995) investigated the frictional behavior of fractures in granite containing a layer of granite powder at hydrothermal conditions. The frictional behavior is divided into two regimes, with the first regime including dry granite up to 845 °C and wet granite below 250 °C, and the second on wet granite above 350 °C. The friction coefficient in the first regime is modestly dependent on temperature, slip rate, and pore pressure. While in the second regime, the friction coefficient shows velocity strengthening and temperature weakening behavior. The results imply that the fluid-assisted deformation processes on fractures in granite are active under high temperature.

Transient changes of pore fluid pressure caused by shear-induced dilatancy or compaction during ongoing shear also have a great impact on the frictional behavior of rock fractures. Samuelson et al. (2009) reported the dilatancy coefficient obtained from velocity-stepping tests (1 to 100 $\mu\text{m/s}$) under effective normal stresses ranging from 0.8 to 20 MPa. They found that the dilatancy coefficient is independent of the effective normal stress and increasing shear strain has little effect on the dilatancy coefficient. However, they did not produce dilatancy-induced depressurization during their tests due to high permeability of the granular quartz used in their tests. They numerically modelled the dilatancy-hardening behavior caused by dilatancy-induced depressurization under seismogenic conditions, indicating that other mechanisms such as thermal pressurization of pore fluid should be involved to nucleate earthquakes in fluid filled fracture zones. Recently, Faulkner et al. (2017) examined the effect of shear-enhanced compaction of low-permeable gouge on laboratory frictional measurements, including frictional strength, rate-and-state friction parameters, and slide-hold-slide results. In nature,

pore fluid overpressure due to shear-enhanced compaction tends to induce fracture slip.

The coupled evolution of friction and permeability of fractures is another topic of great interest, which can also be affected by the mineral composition, fracture roughness, rock type, stress state and slip history. Ikari et al. (2009) performed laboratory tests on water-saturated clay-rich gouge to investigate its frictional behavior and permeability evolution with increasing shear displacement. The friction coefficient is low, and all the clay-rich gouge shows velocity-strengthening behavior. Permeability was found to decrease during shearing. Montmorillonite and illite gouges show lower permeability than chlorite. This indicates that the presence of chlorite may allow the drainage of the gouge layer and thus reduce the pore pressure and increase effective stress. In addition, low pore pressure also favors consolidation and cementation of gouge. It implies that low pore pressure and high effective stress provide a favorable condition for seismic nucleation. Fang et al. (2017) studied the coupled evolution of frictional stability-permeability with increasing shear displacement under simulated in-situ stress and pore pressure states. They found that carbonate-rich shale shows higher frictional strength, and neutral frictional stability, and its permeability reduction is coupled with the friction process, while phyllosilicate-rich shale is characterized by lower frictional strength, and strong frictional stability, and its permeability reduction is caused by mineral swelling and decoupled with the friction process. A more comprehensive study on the mineralogical control of frictional stability and permeability change reveals that frictional stability increases with increasing content of carbonate and clay and decreases with increasing content of tectosilicate. However, the permeability change decreases with increasing frictional stability (Fang, Elsworth, Wang, et al., 2018). A transition from velocity-strengthening, to neutral and finally to velocity-weakening has been observed during the shearing of both rougher and smoother fractures. However, the permeability of smoother fracture decreases monotonically and that of the rougher fracture fluctuates because of the alternation between compaction and dilation (Fang, Elsworth, Ishibashi, et al., 2018). Ishibashi, Elsworth, et al. (2018) investigated the frictional stability of fractures in Westerly

granite under controlled pore pressure and normal stress conditions, and they observed a velocity-weakening behavior accompanied by permeability enhancement.

2.6.4 Injection-driven shear-flow tests on rock fractures

Injection-induced instability of critically stressed fractures is proven to be the dominant source of injection-induced seismicity (Falls et al., 1992; Rutledge et al., 2004). Fluid pressure builds up and then diffuses in the stimulated reservoir, which reduces the effective normal stress on critically stressed fractures, inducing the activation of fractures. Therefore, injection-driven shear tests on critically stressed fractures simulate more directly the injection-induced fracture instability compared to the conventional displacement-driven shear tests.

The injection-induced fracture instability can be complicated by the nonuniform fluid pressure distribution during injection-driven shearing of rock fractures. Rutter and Hackston (2017) investigated slip induced by fluid injection on pre-existing fractures in permeable and impermeable sandstones with or without the presence of fault gouge. They found that if the fluid can only reach the fracture through the borehole, the effective stress law is inapplicable because of the nonuniform distribution of fluid pressure over the fracture. Passelegue et al. (2018) further demonstrated that the fluid pressure heterogeneity is controlled by the balance between fluid injection and diffusion rates. The fluid pressure at failure can be largely underestimated by the Mohr-Coulomb failure criterion when there is a high degree of fluid pressure heterogeneity (Passelegue et al., 2018; Rutter and Hackston, 2017). However, the fracture slip can propagate beyond the fluid pressurized area because the localized fluid pressure acts on the fracture as a point force possibly to trigger the slip of the entire fracture if the point force is enough high (Garagash and Germanovich, 2012; Passelegue et al., 2018).

The injection-induced frictional response of fractures is also dependent on the interplay between frictional and hydraulic properties of the fracture. Scuderi et al. (2017) demonstrated that the velocity strengthening carbonate gouge, which should

favor aseismic creep, can be induced to slip dynamically by fluid pressurization. This happens because the fracture strengthening caused by increasing slip velocity is overcome by the fracture weakening induced by fluid pressurization. Nevertheless, the shale gouge only exhibited a slow injection-induced slip rate upon injection-induced activation due to its extremely low permeability and strong velocity strengthening behavior, indicating that the hydraulic condition, and rate- and-state frictional property collectively control the fracture response to fluid injection (Scuderi and Collettini, 2018).

There are also some attempts to investigate the coupled evolution of frictional slip and permeability of fractures during injection-driven shearing. Ye and Ghassemi (2018) carried out injection-driven shear tests on critically stressed fractures in granite and found that a self-propping shear slip occurred in a stick-slip manner during stepwise fluid pressurization. The irreversible permeability enhancement after injection-driven shear slip indicates that hydraulic stimulation can be an effective stress strategy for enhanced geothermal systems. Ishibashi, Asanuma, et al. (2018) conducted an injection-driven shear test on a critically stressed tensile fracture in granite. They found that half of the permeability enhancement can be achieved by aseismic slip before being activated to slip seismically.

2.6.5 Field fluid injection tests on in-situ fractures

Fluid injection tests on decameter-scale (10 m) fractures in the field bridge the gap between centimeter-scale (cm) laboratory tests and kilometer-scale (km) reservoir stimulation projects. The accurate observation and detailed analysis of fluid injection tests in decameter scale with the aid of advanced instruments could provide fruitful insights into how fluid pressure migrates, how seismic/aseismic deformation/motion of the fracture is induced, and how these coupled processes interact.

The apparatus and injection protocols used in field fluid injection tests on in-situ fractures are shown in Figure 2-9.

Figure 2-9 Test setup of field fluid injection on in-situ fractures (after Guglielmi, Elsworth, et al., 2015). (a) Overview of the equipment setup. (b) Schematic drawing of the three-dimensional deformation unit. (c) Schematic drawing showing the step-rate injection protocol.

As shown in Figure 2-9, a three-dimensional deformation unit is employed to measure the displacement field of the fracture, which can be resolved as the shear and normal displacements. Water is injected through a borehole into an injection chamber isolated by two inflatable packers. The injection flow rate, injection pressure and in-situ temperature are also recorded. A concurrent measurements of these shear-flow characteristics of the fracture can be achieved through these efforts (Guglielmi, Elsworth, et al., 2015). Several field fluid injection tests have been performed on fractures intersecting limestones in the Laboratoire Souterrain à Bas Bruit (LSBB), southeast France (De Barros et al., 2018; Derode et al., 2015; Duboeuf et al., 2017; Guglielmi, Cappa, et al., 2015), shales in Tournemire URL, Southern France (De Barros et al., 2016; Guglielmi, Elsworth, et al., 2015), and clay in Mont Terri Underground Research Laboratory (URL), Switzerland (Jeanne et al., 2017, 2018).

In combination with laboratory tests (Cappa et al., 2019) and numerical modeling (Bhattacharya and Viesca, 2019; Cappa et al., 2018; Cappa et al., 2019; Guglielmi, Cappa, et al., 2015; Guglielmi, Elsworth, et al., 2015; Jeanne et al., 2018; Kakurina et al., 2019), insightful findings are reached from these fluid injection tests on in-situ fractures. Some general insights are summarized here.

1. The interplay among fluid pressure build-up and diffusion, fracture normal and shear deformation, as well as fracture permeability change is complex during field fluid injection tests on in-situ fractures. Particularly, the fracture deformation is controlled by the fracture complexities (Guglielmi et al., 2020). The heterogeneous stress response to fluid injection resulted from the structural heterogeneity causes the complex seismic/aseismic response of fracture, because of the frictional heterogeneity of the fracture (De Barros et al., 2016; Guglielmi, Cappa, et al., 2015).
2. Fluid injection often directly induces aseismic slip of the velocity-strengthening fracture segment, which then triggers the seismic slip of unpressurized fracture segment. This happens because of the accumulated shear stress caused by propagating aseismic slip in the unpressurized area (stress transfer), where the

fracture segment is velocity-weakening (Cappa et al., 2018; Cappa et al., 2019; Guglielmi, Cappa, et al., 2015). The permeability changes in the activated fracture patches are heterogeneous (Cappa et al., 2018; Guglielmi, Elsworth, et al., 2015; Jeanne et al., 2018), while an overall permeability increase is the necessity for accumulating shear stress beyond the pressurized zone (Cappa et al., 2018).

3. The occurrence of seismic events can be independent of fluid diffusion, which is found to be caused by stress perturbation resulted from aseismic motion (De Barros et al., 2018; Duboeuf et al., 2017). Aseismic deformation can be a major mechanism of injection-induced strain energy release on fractures (De Barros et al., 2016; Duboeuf et al., 2017; Guglielmi, Cappa, et al., 2015). In other words, the release of injection-induced seismic moment can be accommodated by aseismic slip within and beyond the pressurized zone. Therefore, the current prediction of maximum seismic moment based on injection-induced stress change (McGarr, 2014) and stimulated reservoir volume (Shapiro et al., 2011) may overestimate the maximum magnitude of injection-induced earthquake.

2.7 Summary

This chapter reviews the basic theories related to the shear-flow characteristics of rock fractures, including Mohr-Coulomb failure criterion commonly used to describe rock failure, friction laws used to discuss fracture stability, earthquake source parameters used to quantify fracture slip, and cubic law used to depict fluid flow through fractures. A comprehensive review of previous work on this topic has also been provided. Several concluding remarks can be drawn:

1. The linearized Mohr-Coulomb failure criterion, the slip-weakening and rate-and-state friction laws, the earthquake source parameters, and the cubic law can reasonably and satisfactorily describe the frictional strength, frictional stability, fracture slip, and fluid flow of rock fractures, respectively. The analysis of injection-induced frictional slip based on friction laws suggests contradictory sliding behaviors, implying the poor understanding of the mechanism of injection-induced seismicity.

2. Although extensive studies have been conducted to investigate the frictional characteristics, flow characteristics, and displacement-driven shear-flow characteristics of rock fractures, they are not directly simulating the injection-driven shear-flow characteristics of rock fractures. While there is a continuing interest in the mystery of injection-induced seismicity, only a handful of experimental and field studies have been carried out to examine the evolution of frictional stability and permeability of rock fractures during fluid injection. This comprehensive and state-of-the-art review identifies some gaps in current research: First, the effects of fracture surface heterogeneity and fluid pressure heterogeneity on injection-induced fracture activation and seismic moment release remains enigmatic. Second, the underlying mechanism of seismic risk mitigation by cyclic fluid injection is yet to be specified.

CHAPTER 3 LABORATORY FACILITIES AND METHODOLOGY

3.1 Introduction

In this chapter, laboratory facilities including MTS 815 triaxial rock mechanics test system, Vindum VP-12K dual syringe pumps, PAC (Physical Acoustic Corporation) acoustic emission (AE) system, and David three-dimensional (3D) scanner have been introduced, with emphasis on their specifications and working principles. The experimental configuration and design of triaxial shear-flow setup are also detailed, followed by discussion of the comprehensive corrections to accommodate the dynamic changes of the test system and sample assembly during the tests. The reliability of the triaxial shear-flow setup is demonstrated by measuring the frictional strength of sawcut fractures in granite.

3.2 Experimental facilities

3.2.1 MTS 815 triaxial rock mechanics test system

The triaxial shear-flow tests are performed using MTS 815 triaxial rock mechanics test system (Figure 3-1a), which can create geological environments of various states of stress and pore pressure. The system consists of a high stiffness loading frame with an axial actuator, a triaxial cell, a confining pressure intensifier, and a pore pressure intensifier, a digital controller and a hydraulic power unit (Figure 3-1b). The digital controller provides a high-speed closed-loop control of intensifiers and axial actuator (Figure 3-2). Particularly, the controller compares the function generated command with the feedback signal, which can be either displacement, stress, or strain, to provide an error signal proportional to their difference. The error signal then becomes the servo-valve command according to the proportional, integral and derivative feedback algorithms, which drives the actuator to compensate for the difference until the error between the function generated command and feedback signal reaches zero. Table 3-1 lists the detailed information of the five main components of MTS 815 triaxial rock mechanics test system.

(a)

(b)

Figure 3-1 MTS 815 triaxial rock mechanics test system. (a) Photo of MTS 815 triaxial rock mechanics test system, (b) Schematic diagram of MTS 815 triaxial rock mechanics test system (after MTS manual, 2004).

Figure 3-2 Illustrative diagram showing the principle of high-speed closed-loop control of MTS 815 triaxial rock mechanics test system.

Table 3-1 Specifications of MTS 815 triaxial rock mechanics test system.

(1) MTS 505.07 Hydraulic Power Unit
Flow rate: 22.7 ltm (litre per minute)
Output pressure: 21 MPa
Required electricity: 400 VAC, 3 phase, 50 Hz
(2) MTS 315.02 Load Frame
Stiffness: 9.0×10^9 N/m
Compression rating: 2700 kN
Tension rating: 1350 kN
Actuator stroke: 100 mm
(3) MTS 656.05 Triaxial Cell
In-vessel load cell rating: 2600 kN
Confining pressure rating: 140 MPa
Pore pressure rating: 140 MPa
Rated operating temperature: 200 °C
(4) MTS 286.20-09 Confining Pressure Intensifier
Maximum output pressure: 140 MPa
Maximum output volume: 40 in ³
Piston diameter: 2.5 inch
Confining fluid: Silicon oil
Maximum fluid temperature: 75 °C
(5) MTS 286.31-03 Transient Pulse Pore Pressure Intensifier
Capable of permeability measurement by steady-state flow method and pulse-decay method
Maximum output pressure: 140 MPa
Maximum output volume: 20 in ³
Piston diameter: 2.5 inch
Pore fluid: Distilled water
Maximum fluid temperature: 75 °C

3.2.2 Vindum VP-12K dual syringe pumps

MTS 286.31-03 Transient Pulse Pore Pressure Intensifier provides only one pore pressure transducer, which is not always enough for the purposes of this study. Therefore, the Vindum VP-12K dual syringe pumps manufactured by the Vindum Engineering Inc., United States have also been used, which are equipped with two parallel syringe cylinders (Figure 3-3). Each cylinder has a single stroke volume of 10 mL and can supply up to 82.7 MPa fluid pressure with various working fluids such as aqueous, oils, brines, and most gases. The maximum and minimum flow rates are 30 mL/min and 0.0001 mL/min, respectively. The flow rate accuracy and resolution are $\pm 0.1\%$ of the set flow rate and 0.000001 mL/min, respectively. The volume accuracy and resolution, pressure control accuracy and resolution are $\pm 0.1\%$ of the pumped volume, 0.000001 mL, ± 2 psi (~ 13.7 kPa) and 0.1 psi (~ 0.69 kPa), respectively.

Figure 3-3 Photo of Vindum VP-12K dual syringe pumps.

3.2.3 Acoustic emission system

The rapid release of strain energy stored in a stressed material produces elastic waves, known as AE events, which directly reflect the micro-crack generation and propagation (Lockner, 1993). This phenomenon is analogous to the micro-seismicity (MS) induced by excavation, extraction, and/or fluid injection in the engineering scale (McGarr et al., 2002; Scholz, 1968). Therefore, AE monitoring technique has been extensively employed in rock mechanics and geophysics due to its high sensitivity, real-time measurement, and source localization capability (Ishida et al., 2017).

Lockner (1993) reviewed the application of AE monitoring technique in understanding rock failure. Lei and Ma (2014) summarized the role of AE monitoring technique in investigating the earthquake generation process. Lockner et al. (1991) presented a detailed view of fracture nucleation and propagation in Westerly granite by controlling the axial stress to achieve a constant AE rate. He et al. (2010) investigated the AE characteristics of a series of single-face dynamic unloading tests on limestone under true-triaxial conditions. It is found that the AE signals near unloading-induced burst are characterized by higher-amplitude and lower-frequency, and the release of AE energy increases abruptly towards failure. McLaskey et al. (2012) reported a suite of laboratory study on AE characteristics during the rupture of fracture after different degrees of healing and found that the increasing fracture healing likely produces high-frequency AE signals. McLaskey and Lockner (2014) analyzed the AE events before and during the stick-slip instabilities of sawcut fractures in granite. They found that AE events before stick-slip are associated with the mostly aseismic premonitory fracture slip preparing for the large instability, that is, they are resulted from the nucleation process of stick-slip. Lei, Li, et al. (2017) investigated the AE characteristics during fracturing of unfavorably oriented fractures by locating the hypocenters. The results suggested that there are two competing processes involving in the fracturing of unfavorably oriented fractures: on the one hand, stick-slip events smoothen the fracture surface; on the other hand, new sub-fractures roughen the fracture surface.

In this study, an overview of the AE system manufactured by PAC combined with MTS 815 triaxial rock mechanics test system is shown in Figure 3-4. The AE system with 16 available channels allows acoustic emission data acquisition in a high ambient noise environment. The AE signal can be transferred at a speed of up to 132 Mb/s, and the detectable frequency can range from 1 to 400 kHz. Three ceramic piezoelectric sensors (resonance-type) are attached onto the axial column using grease as the coupling medium. The pre-amplitude gain and the amplitude threshold are set to 40 dB. Using this setup, the accompanying AE signals during the triaxial shear-flow tests can be obtained.

Figure 3-4 Overview of the AE system combined with MTS 815 triaxial rock mechanics test system.

The detailed specifications of each part are described in the following. The ceramic piezoelectric sensors are resonance-type sensors (Nano 30 Sensor), which have a resonant response at 300kHz and show a good frequency response over a broad frequency range from 125 to 750 kHz. The sensor is of small size connecting with

an integral coaxial cable equipped with a Bayonet Neill Concelman (BNC) connector at the other end (Figure 3-5a and c). To improve the contact quality between the sensor and column for better detection of low level acoustic signals, couplant of low viscosity is used to fill the gaps between two surfaces (Figure 3-5b).

Figure 3-5 Details of AE sensor. (a) The resonance-type sensor connecting with an integral coaxial cable equipped with a BNC connector at the other end. (b) Couplant used to insure good contact between the sensor and column. (c) A diagram showing dimensions of the sensor.

A pre-amplifier is used to amplify the original signal detected by the sensor (Figure 3-6). This pre-amplifier provides 20/40/60 gain, which is switch selectable according to the sample properties, requirements of sensor and background noise.

Figure 3-6 The 20/40/60 Pre-amplifier.

The terminologies (Figure 3-7) used in the AE monitoring technique are:

1) Time of hit (trigger time): The time when the AE system detects the hit, i.e., the amplitude of the AE signal exceeds the AE threshold level.

- 2) AE threshold: The AE discriminate level that is set in advance considering both the frequencies of anticipated waves and ambient noise.
- 3) Amplitude: The maximum amplitude of the AE signal during an AE hit. The amplitude can be calculated as $\text{dB} = 20 \log (V_{\max}/1\mu\text{-volt}) - (\text{Pre-amplifier Gain in dB})$, where $V_{\max}$ is the maximum voltage.
- 4) Rise time: The time interval between the time of hit and the time when the AE signal reaches the peak amplitude.
- 5) AE duration: The time interval between the first and the last threshold crossing of the AE signal from the AE threshold.
- 6) AE counts: The number of times that the AE signal exceeds the AE threshold. In the case shown in Figure 3-7, four counts are produced for this AE hit. It is noteworthy that AE counts here is the so-called ring-down counts.
- 7) AE Energy: The Root Mean Square (RMS) value of varying AE signal “voltage” during an AE hit is representative of the AE energy (Ishida et al., 2017). The sum of RMS values (of relative unit) from three sensors with sufficient coverage can reduce the directional bias of AE sensor (Figure 3-4a).

Figure 3-7 An example waveform of an AE hit.

3.2.4 Three-dimensional scanner

The fracture surface topography is a significant factor that influences the shear-flow characteristics of rock fractures. In this study, the David 3D scanner uses the structured light scanning (SLS) technology to precisely image the fracture surface

topography (Figure 3-8). The HP camera captures the 3D models of physical objects based on the light emitted by the projector. The scan size ranges from 60 to 500 mm, and in a matter of seconds, the scanner captures the reality with a three-dimension scan resolution of up to 0.05 mm. With the combination of a self-coded Matlab program, the 3D topography of fracture surface can be constructed.

Figure 3-8 The David 3D Scanner. (a) Photo of David 3D Structured Light Scanner Pro S3 (b) Diagram of the David 3D scanner (after David 3D Scanner manual).

There is a vast variety of surface roughness quantification methods (Gadelmawla et al., 2002; Magsipoc et al., 2019; Tse and Cruden, 1979). Among the various methods of surface roughness quantification, the Root Mean Square (RMS) asperity height (R_q) describing the RMS value of roughness amplitude is a straightforward parameter to measure the fracture surface roughness, which can be calculated as (Gadelmawla et al., 2002),

$$R_q = \sqrt{\frac{1}{n} \sum_{i=1}^n Z_i^2} \quad (3-1)$$

where Z_i is the sampling asperity height on the fracture surface.

3.3 Triaxial shear-flow setup

Several test setups have been reported to investigate the shear-flow characteristics of rock fractures, including the direct shear-flow setup (Esaki et al., 1999; Fang et al., 2017; Park et al., 2013; Zhang, Li, et al., 2019), double direct shear-flow setup (Collettini et al., 2014; Ikari et al., 2009), triaxial shear-flow setup (Passelegue et al., 2018; Wu et al., 2017), and rotary shear-flow setup (Ma et al., 2014; Passelègue et

al., 2016). Among these setups, the triaxial shear-flow setup can use the conventional triaxial test setup, which is accessible in many universities and institutions globally, combined with commercially ready high-pressure fluid pumps.

Figure 3-9 Methods of introducing fluid flow into an inclined fracture. (a) Two boreholes drilled through the short edges of forcing blocks. (b) Two boreholes deviated from the center of block ends to both sides of the fracture. (c) A borehole drilled from the center of one block end and split into three branches to the fracture. (d) A borehole drilled through the short edge of the lower block. (e) A pair of porous blocks without drilled boreholes. The white lines indicate drilled boreholes.

The triaxial shear-flow tests can be conducted by preparing an inclined fracture in a cylindrical sample and introducing pore fluid to the fracture plane. The forcing blocks can be hardened steels and hard rocks. The angle of fracture plane with respect to the horizontal plane is typically 45° or 60° . Pore fluid can be introduced into the fracture plane using different methods (Figure 3-9): two boreholes drilled through the short edges of forcing blocks, e.g., Ye and Ghassemi (2018) (Figure 3-9a), two boreholes deviated from the center of the block ends to the two sides of the fracture plane, e.g., Rutter and Mecklenburgh (2018) (Figure 3-9b), a borehole drilled from the center of one block end and split into three branches connecting to the fracture plane, e.g., Marone and Scholz (1989) (Figure 3-9c), a borehole drilled through the short edge of the lower block, e.g., Passelegue et al. (2018) (Figure 3-9d), and a pair of porous blocks without drilled boreholes, e.g., Tembe et al.

(2010) (Figure 3-9e). Among these methods, the methods shown in Figure 3-9a and Figure 3-9b have an advantage of measuring fracture permeability before, during and after the fracture slip, because there are outlet and inlet at the top and bottom edges of the fracture, respectively.

3.3.1 Experimental configuration

The triaxial shear-flow tests in this study are conducted on fractures manufactured from Bukit Timah granite. The characterization of this granite can be found in the Appendix, including microscopical observation, physical, mechanical, and hydraulic properties. The sawcut fracture is prepared by cutting a 50-mm-diameter and 100-mm-height cylindrical sample at 30° to the core axis using a diamond saw. The sawcut surface is then polished using fine sandpaper with a particle size of $25.6\ \mu\text{m}$. Two boreholes with a diameter of 2 mm are drilled parallel to the core axis through the short edges of the sample halves to facilitate fluid flow from the coreholders to the fracture plane. The preparation procedures for the sawcut fracture, including cutting, machine grinding, hand-lapping, jacketing and assembling are shown in Figure 3-10. Favorably oriented natural fractures can also be tested using the triaxial shear-flow setup, for which the preparation procedures will be detailed in appropriate contexts.

Figure 3-11 shows the triaxial shear-flow setup. The lower coreholder serves as pore fluid inlet, while the upper coreholder only works as inlet when injecting pore fluid from both boreholes, but it is outlet when measuring the permeability or purely monitoring the fluid pressure in the top borehole. Two layers of heat shrinkable Teflon jacket in the sample assembly are used. The first layer is to stabilize the fractured sample, and the second is to fix the sample on the coreholders. The two layers of Teflon jackets exclude the confining oil. A piece of filter paper with a pore size of $0.45\ \mu\text{m}$ and a thickness of $130\ \mu\text{m}$ is inserted in the interfaces between the sample ends and the coreholders to prevent sheared-off particles flowing into the pore system.

The triaxial shear-flow tests are performed using MTS rock mechanics test system at ambient temperature (25 °C). The axial loading capacity of the system is 2600 kN with an accuracy of $\pm 0.3\%$. The confining and pore systems can provide high pressures of up to 140 MPa with an uncertainty of $\pm 0.3\%$. Silicon oil and distilled water are used in the confining and pore systems, respectively. The shear stress (τ) and normal stress (σ_n) applied on the inclined fracture can be expressed in terms of the principal stresses (σ_1 and σ_3) (Figure 3-12),

$$\tau = \frac{1}{2}(\sigma_1 - \sigma_3)\sin 2\beta = (\sigma_1 - \sigma_3)\sin\beta\cos\beta \quad (3-2)$$

$$\sigma_n = \frac{1}{2}[(\sigma_1 + \sigma_3) - (\sigma_1 - \sigma_3)\cos 2\beta] = \sigma_3 + (\sigma_1 - \sigma_3)\sin^2\beta \quad (3-3)$$

where, $\sigma_1 - \sigma_3$ is the deviator stress measured directly by an in-vessel load cell; σ_1 and σ_3 are the axial stress and confining pressure, respectively; β is the fracture inclination angle with respect to the horizontal plane. The normal and shear stresses on the fracture were directly controlled by servo-controlling the deviator stress and confining pressure.

Figure 3-10 Preparation procedures for the sawcut fracture, including (1) cutting, (2) machine grinding, (3) drilling, (4) hand-lapping, (5) jacketing, and (6) assembling.

(a)

(b)

Figure 3-11 Triaxial shear-flow setup housed inside a pressure vessel. (a) Photo of the sample-coreholder assembly. (b) Illustration of the assembly. The red arrows indicate the direction of fluid flow.

Figure 3-12 Schematic diagram of the fractured sample assembled in the triaxial shear-flow setup and the state of stress on the sample.

3.3.2 Experimental design

Although the triaxial shear-flow tests can be readily conducted using a conventional triaxial test setup combined with high-pressure fluid pumps, special attentions must be paid to the test design. Particularly, the fracture inclination angle must be optimized to meet the conditions that the frictional slip occurs along the fracture and the rock matrix keep intact. According to the variation of rock strength with fracture inclination angle (Figure 3-13), the frictional slip along the fracture cannot occur when the fracture inclination angle is between 0° and φ , where φ is the internal friction angle of the intact rock, and when the fracture inclination angle approaches 90° . When the rock sample fails along the fracture, the optimal inclination angle is $45+\varphi/2$. The internal friction angle of Bukit Timah granite is 54° (Appendix A). Thus, the optimal inclination angle is 72° . In the tests, an inclination angle of 60° is adopted, which is easy to be prepared and commonly used in other studies (Passelegue et al., 2018; Wu et al., 2017; Ye and Ghassemi, 2018).

Figure 3-13 Rock strength at constant confining pressure as a function of angle to the direction of confining pressure (after Brady and Brown, 2013).

The triaxial shear-flow tests consider fluid flow in rock fracture without interacting with adjacent matrix. In other words, the permeability of rock matrix must be negligible comparing to that of the fracture. The same test setup is used to measure the permeability of an intact sample and the same sample after introducing a sawcut fracture using the pulse-decay technique under various combinations of confining and fluid pressures. The details of permeability measurement of granite matrix can be found in Appendix A. The permeability of the fractured sample (Figure 3-14) is approximately 1000 times that of the intact sample (Figure A-7), implying that the granite matrix can be considered as non-permeable in the triaxial shear-flow tests. The permeability contrast between the granite matrix and sawcut fracture satisfies the assumption that the fluid solely flows through the fracture.

Figure 3-14 Permeability of a granite sample containing a sawcut fracture under various combinations of normal stress and pore pressure.

3.3.3 Experimental correction

The corrections on the triaxial shear-flow setup are critical to accommodate the dynamic changes of the testing system and sample assembly during the tests. The system operation and data analysis must consider the changes, including the stiffness of system and rock blocks, fracture contact area, jacketing material deformation, sample end friction and stress concentration on the fracture.

3.3.3.1 Test system stiffness

The load point displacement of the test system and the fractured sample is measured using a linear variable differential transformer (LVDT) installed inside the axial piston in the test system. In the triaxial shear-flow tests, the load point displacement consists of the shear displacement along the fracture, and the elastic deformation of the test system and the two rock blocks. To calculate the shear displacement along the fracture, it is necessary to subtract the displacement associated with the deformation of the test system and the rock blocks from the load point displacement. The deformation of the test system can be assessed based on the stiffness of the test system, and the deformation of the rock blocks can be estimated based on the Young's modulus of the granite.

The stiffness of the test system is measured by conducting the uniaxial compression test on a cylindrical aluminum sample with 100 mm in length and 50 mm in diameter. The deformation of the test system is obtained by subtracting the axial deformation of the aluminum sample from the load point displacement. The stiffness of the test system is 669.9 kN/mm, obtained from the slope of the deviator force versus system deformation curve (Figure 3-15). The non-linear increase of the curve at the early stage has been reported in previous studies on machine stiffness (Collettini et al., 2014), which is probably due to the low tightness of the test system under low stress conditions.

The elastic deformation of the rock blocks is estimated from the Young's modulus of the granite. The effect of confining pressure on the stiffness of the rock blocks is not considered, as the Young's modulus increases with confining pressure at an extremely low rate of 0.02 GPa/MPa (Figure A-5b). Taking an average Young's modulus of 73.8 GPa, and a standard deviation of 0.88 GPa, the stiffness of the rock blocks with 100 mm in length and 50 mm in diameter is 1447 kN/mm with an accuracy of 1.2%. Hence, the shear displacement along the fracture (dl_s) can be obtained by the load point displacement (Δl) subtracting the deformation of the test system and the rock blocks,

$$dl_s = \frac{dl}{\sin \beta} = \frac{\Delta l - \frac{\Delta F}{K_m} - \frac{\Delta F}{K_b}}{\sin \beta} \quad (3-4)$$

where dl is the corrected axial displacement, i.e., the vertical movement of the axial piston due to the fracture slip; ΔF is the deviator force; K_m is the stiffness of test system; K_b is the stiffness of rock blocks.

Figure 3-15 Deviator force as a function of the elastic deformation of test system. The slope is the system stiffness.

3.3.3.2 Fracture contact area

In the triaxial shear-flow tests, the contact area between the rock blocks decreases with increasing axial displacement. In order to control precisely the normal and shear stresses on the inclined fracture, the reduction in contact area must be considered in the tests. Before the onset of fracture slip (Figure 3-16a), the initial contact area (A_0) can be calculated from the projected area on the horizontal plane,

$$A_0 = \pi \left(\frac{D_s}{2} \right)^2 / \cos \beta \quad (3-5)$$

where D_s is the sample diameter.

Figure 3-16b shows schematically the reduction of contact area after a certain corrected axial displacement (dl). Θ is the angle subtended by the arc A_1A_2 at the center of the circle O_2 . A_1 and A_2 are the two intersection points of the two circles. The area of the sector $A_1A_2O_2$ is,

$$S = \frac{1}{2} \Theta \left(\frac{D_s}{2} \right)^2 \quad (3-6)$$

and the area of the triangle $A_1A_2O_2$ is,

$$S_{\Delta} = \frac{1}{2} \sin \Theta \left(\frac{D_s}{2} \right)^2 \quad (3-7)$$

Figure 3-16 Schematic diagram of area reduction during the triaxial shear-flow tests. (a) Initial state. (b) Area reduction after a certain corrected axial displacement.

The projected area of the true contact area on the horizontal plane is an “ellipse-like” area bounded by the two arcs sharing a common chord A_1A_2 . The projected area can be calculated as,

$$S_p = 2(S - S_{\Delta}) = \left(\frac{D_s}{2} \right)^2 (\Theta - \sin \Theta) \quad (3-8)$$

The true contact area can be obtained as,

$$A_{corr} = S_p / \cos \beta \quad (3-9)$$

The area correction coefficient, A_{corr}/A_0 , defined as the ratio of the corrected contact area to the initial contact area is,

$$A_{corr} / A_0 = (\Theta - \sin \Theta) / \pi \quad (3-10)$$

In the triangle A_1OO_2 , the angle Θ can be obtained as,

$$\cos \frac{\Theta}{2} = \sin\left(\frac{\pi}{2} - \frac{\Theta}{2}\right) = \frac{\frac{1}{2} dl \cot \beta}{\frac{1}{2} D_s} = \frac{dl \cot \beta}{D_s} \quad (3-11)$$

$$\Theta = \pi - 2 \arcsin \frac{dl \cot \beta}{D_s} \quad (3-12)$$

By substituting Eqs. (2-11) and (2-12) into Eq. (2-10), the relationship between the area correction coefficient and corrected axial displacement is demonstrated in Figure 3-17.

Figure 3-17 Area correction coefficient as a function of corrected axial displacement.

As shown in Figure 3-17, the area correction coefficient decreases with increasing corrected axial displacement at a nearly constant rate of 2.1%/mm. In the triaxial

shear-flow tests, the corrected shear (τ_{corr}) and normal (σ_{corr}) stresses on the fracture are directly controlled,

$$\tau_{corr} = \tau / (A_{corr}/A_0) \quad (3-13)$$

$$\sigma_{ncorr} = \sigma_n / (A_{corr} / A_0) \quad (3-14)$$

3.3.3.3 Jacketing material deformation

The deformation of jacketing material used in the triaxial shear-flow tests can induce an unexpected increase in deviator force, which may result in the overestimation of the friction coefficient of rock fractures. The increase in deviator force due to jacket deformation can be estimated by conducting a displacement-driven shear test on a rock fracture with negligible shear resistance. The uniaxial compression test on a granite sample with a sawcut fracture filled with a layer of silicon grease is carry out at an axial displacement rate of 1 $\mu\text{m/s}$. It is assumed that the friction of the sawcut fracture filled with silicon grease is nearly zero, and attribute the increase of deviator force to the deformation of jacketing material.

Figure 3-18 Deviator force as a function of load point displacement in the uniaxial compression test on a granite sample with a sawcut fracture filled with silicon grease.

As shown in Figure 3-18, the deviator force induced by the deformation of jacketing material increases approximately linearly with load point displacement at an increment rate of 0.75 kN/mm. The deviator force due to jacket deformation is calculated based on the load point displacement and subtracted from the measured deviator force in the triaxial shear-flow tests.

3.3.3.4 Sample end friction

In the triaxial shear-flow tests, the fracture slip is accompanied by the lateral offset of the two rock blocks. The upper block is held in place by a dowel pin, and the lateral offset can only be accommodated by the lower block. To reduce the end friction during the lateral movement of the lower block, a steel plate under the lower coreholder is inserted and fixed by a dowel pin (Figure 3-19a). The lateral offset between the lower block and the steel plate can induce additional forces on the fracture. Figure 3-19a shows the force balance analysis on the lower block. The shear and normal forces are denoted by red arrows, while the additional forces imposed by the lateral movement are indicated by green arrows. To determine the force induced by the lateral movement of the lower block, a series of displacement-driven shear tests are conducted at an axial displacement of 1 $\mu\text{m/s}$ consecutively on a granite sample with a sawcut fracture filled with a layer of silicon grease under 2.5, 5, 7.5 and 10 MPa normal stresses (Figure 3-19b). The corrections on stiffness, contact area, and jacket deformation in the tests are considered. For the deviator force versus load point displacement curve obtained under each normal stress, the y-intercept is the deviator force required to initiate fracture slip.

(c)

Figure 3-19 (a) Force balance analysis of the lower block. f is the sample end friction. (b) Deviator force as a function of load point displacement under four confining pressures to determine the sample end friction. (c) Friction coefficient inversion by static equilibrium analysis using the results obtained from (b).

At the onset of fracture slip, the following equation by static equilibrium analysis is obtained,

$$\mu_1(\sigma_n + \frac{\mu_2 \Delta \sin \beta}{2A_s}) = \frac{\frac{1}{2} \Delta \sin(2\beta)}{A_s} - \frac{\mu_2 \Delta \cos \beta}{2A_s} \quad (3-15)$$

where, μ_1 is the static friction coefficient of the sawcut fracture filled with silicon grease; μ_2 is the static friction coefficient of the lower block-steel plate interface; A_s is the cross-sectional area of the sample; and Δ is the y-intercept of the deviator force versus load point displacement curve. The reduction of contact area is neglected in Eq. (3-15), since 5% of the initial contact area is reduced at the end of the four tests.

The relationship between the y-intercept and normal stress is obtained by substituting the values of A_s and β and rearranging Eq. (3-15),

$$\Delta = \frac{\mu_1}{0.22 - \mu_2 \times (0.13 - 0.22 \times \mu_1)} \sigma_n \quad (3-16)$$

The two friction coefficients can be obtained by fitting Eq. (3-16) to the data points obtained from the displacement-driven shear tests under the five normal stresses of 0, 2.5, 5, 7.5 and 10 MPa (Figure 3-19c). The data point at 0 MPa normal stress is obtained from the uniaxial compression test on a granite sample with a sawcut fracture filled with a layer of silicon grease at an axial displacement rate of 1 $\mu\text{m/s}$. The static friction coefficient of the sawcut fracture with silicon grease is 0.0387, which is much higher than the static friction coefficient of the lower block-steel plate interface, 0.0009. Thus, the contribution of the static friction coefficient of the sawcut fracture with silicon grease to the increase of deviator force is neglected, since the normal stress on the fracture in the uniaxial compression test is extremely small. Also, the friction coefficient of the lower block-steel plate is too small to consider the effect of sample end friction.

3.3.3.5 Stress concentration

The fracture slip may induce stress concentration around the boreholes drilled at the short edges of the rock blocks during the triaxial shear-flow tests. The lateral movement of the rock blocks may also cause stress concentration at the block edges. The stress concentration in the tests is evaluated based on the finite element method (FEM) using Abaqus software. To simplify the evaluation, the fluid flow is not considered in the sawcut fracture. The cylindrical sample with a diameter of 50 mm and a height of 100 mm in the FEM model is composed of two rock blocks with a sawcut fracture inclined 60° to the horizontal plane. A borehole with a diameter of 2 mm is introduced near the short edge of each rock block. The distance from the center of a borehole to the short edge is 3 mm. It is assumed that the granite is linearly elastic and follows the Mohr-Coulomb failure criterion in the model. The mechanical parameters used in the model are obtained from a series of triaxial compression tests on the intact granite samples (Table 3-2). The friction coefficient of the sawcut fracture is 0.83. In the FEM model, the bottom of the lower block is fixed, and the confining pressure is applied to the sample. The fracture slip occurs

with the axial shortening along the sample axis, and the axial deformation of the rock blocks is negligible. The axial shortening is equivalent to the corrected axial displacement in the triaxial shear-flow tests. As shown in Table 3-3, the numerical simulation under nine different boundary conditions are conducted by systematically changing the normal stress (i.e., 5, 10 and 20 MPa) and axial shortening (i.e., 1, 5 and 10 mm).

Table 3-2 Physical parameters of Abaqus model.

Young's modulus (GPa)	Cohesion (MPa)	Friction angle (°)	Poisson's ratio	Friction Coefficient of interface
73.8	38	54	0.15	0.83

Table 3-3 Boundaries of Abaqus model.

σ_1 (MPa)	σ_3 (MPa)	σ_n (MPa)	τ (MPa)	Axial Shortening (mm)
8	4	5	4.15	1
				5
				10
16	8	10	8.3	1
				5
				10
32	16	20	16.6	1
				5
				10

Figure 3-20 shows the stress distribution on the lower block in the form of the von Mises stress. The stress distribution on the upper block is similar under the same stress condition. The homogeneously stressed area dominates the fracture surface area except the areas around the boreholes and the short edges. The size of the surface area reduces slightly with increasing axial shortening. The magnitude of homogeneous stress increases with higher normal stress. When the sample experiences different magnitudes of axial shortening under the same normal stress level, the magnitudes are comparable. The Mises stress along the major axis of the sawcut fracture shows that the peak values appear at the two sides of the fracture (Figure 3-21), which is due to the stress concentration caused by the drilled borehole and axial shortening. The plateau between the two peaks of the Mises stress reflects the slight reduction of homogeneously stressed area with larger axial

shortening. Although increasing axial shortening enlarges the stress concentration area at two sides of the fracture, most of the surface area is still uniformly stressed. The Mises stress increases slightly at the distance ranging from 4 to 8 mm, implying that the stress concentration caused by the drilled boreholes are limited due to the small size of the boreholes. Therefore, the impact of stress concentration on the fracture in the triaxial shear-flow tests is ignored.

Figure 3-20 Mises stress (MPa) distribution contours on the lower rock block. (a) 1 mm axial shortening under 5 MPa normal stress. (b) 1 mm axial shortening under 10 MPa normal stress. (c) 1 mm axial shortening under 20 MPa normal stress. (d) 5 mm axial shortening under 5 MPa normal stress. (e) 5 mm axial shortening under 10 MPa normal stress. (f) 5 mm axial shortening under 20 MPa normal stress. (g) 10 mm axial shortening under 5 MPa normal stress. (h) 10 mm axial shortening

under 10 MPa normal stress. (i) 10 mm axial shortening under 20 MPa normal stress.

(a)

(b)

(c)

Figure 3-21 Evolution of Mises stress along the major axis of the sawcut fracture under three normal stresses, i.e., 5 MPa, 10 MPa and 20 MPa, at an axial shortening of (a) 1 mm, (b) 5 mm and (c) 10 mm. The zero datum is taken at the lower edge.

3.3.4 Frictional strength of sawcut fractures in granite

To demonstrate the reliability of the triaxial shear-flow setup after corrections, a series of displacement-driven shear tests using the triaxial shear-flow setup on the sawcut fracture with and without the presence of distilled water were conducted. For the displacement-driven shear tests without the presence of pore water, the shear strengths are measured under 5, 7.5, 10 and 12.5 MPa normal stresses. With the presence of 1 MPa pore pressure, the shear strengths are measured under 5, 6, 7.5, 11 and 21 MPa normal stresses. These displacement-driven shear strengths are compiled to derive the friction coefficients by neglecting the shear hardening effect and cohesion. The results show that the friction coefficient measured with the presence of pore water is 0.59, lower than 0.83 measured without the presence of pore water (Figure 3-22). The reduction of friction coefficient of rock fractures caused by the presence of pore water has been observed in previous studies (Byerlee, 1967; Zhao et al., 2017), which is attributed to the reduction of contact surface energy (Byerlee, 1967; Dieterich and Conrad, 1984). Both the friction coefficients are compatible with the Byerlee's law of rock friction, which

generalizes the friction of rock fractures to a value between 0.6 and 0.8 (Byerlee, 1978).

Figure 3-22 Mohr-Coulomb failure envelopes of the sawcut fracture with and without the presence of pore water. The friction coefficient reduces from 0.83 under the dry condition to 0.59 with the presence of pore water.

3.4 Summary

The main facilities used in this study, including MTS 815 triaxial rock mechanics test system, Vindum VP-12K dual syringe pumps, PAC AE system, and David 3D scanner, have been introduced in this chapter. This is followed by a comprehensive study on the fundamental issues involved in the design and corrections of the triaxial shear-flow setup. The reliability of the triaxial shear-flow setup after corrections is then demonstrated by measuring the frictional strength of the sawcut fracture in Bukit Timah granite. The internal friction angle of intact rock determines the optimal fracture inclination angle to prevent fracturing of the rock matrix. The fluid flows solely through the fracture because the matrix permeability is far less than that of the fracture. The results of calibration tests show that the effects of the test system stiffness, reduction in fracture contact area, jacketing material deformation should be considered, while the sample end friction is negligible. The

negligibility of the stress concentration caused by the drilled boreholes and axial shortening is also demonstrated using numerical modeling based on Abaqus. The friction coefficient of the sawcut fracture is measured and compared with the Byerlee's law of rock friction, which validates the reliability of the corrected triaxial shear-flow setup.

CHAPTER 4 LABORATORY INVESTIGATION ON INJECTION-INDUCED INSTABILITY OF CRITICALLY STRESSED FRACTURES

4.1 Introduction

The current understanding of injection-induced seismicity comes primarily from the observation and analysis of unintended seismic activities induced by fluid injection (Keranen et al., 2014; Walsh and Zoback, 2015). However, the ability to predict the moment magnitude of injection-induced earthquakes is woefully inadequate. Recent insights into the mechanism of fracture instability suggest forecasting the moment magnitude of the anthropogenic earthquakes based on the minimum principal axis of fluid-stimulated rock volume (Shapiro et al., 2011), the total volume of injected fluid (Galis et al., 2017; McGarr, 2014; McGarr and Barbour, 2018) and the Gutenberg-Richter sampling statistics (van der Elst et al., 2016). Particularly, the upper bound of seismic moment release proposed by McGarr and Barbour (2018) is applicable to the aseismic and seismic events associated with wastewater disposal, enhanced geothermal systems, hydraulic fracturing and field field injection tests. However, this model is built on several assumptions, such as uniform distribution of fluid pressure and half of shear stress drop from the shear strength. The maximum arrested moment of the induced earthquakes can be estimated as the injected volume multiplied by a reservoir parameter, but the estimation of reservoir parameter is very challenging (Galis et al., 2017). Hence, further verification and comprehensive consideration are needed to improve the prediction of the complicated seismic events. In addition, after the injection-induced fracture instability is successfully simulated in the laboratory (Passelegue et al., 2018; Scuderi and Collettini, 2018; Scuderi et al., 2017; Wang et al., 2020), bridging the gap between the experimental and field studies is the next frontier for improving the understanding of injection-induced seismicity.

The objective of this chapter is to experimentally investigate the mechanism of injection-induced instability of critically stressed fractures in granite. A series of triaxial shear-flow tests is conducted on sawcut and natural fractures approaching a critical stress state, and examine fluid pressure heterogeneity, injection-induced fracture instability, acoustic emission characteristics and fracture surface evolution.

Several physical processes controlling injection-induced fracture instability are discussed, including friction loss, thermal pressurization, and stress imbalance. Finally, the magnitudes of moment release suggested by various models with experimental and field data are compared to further improve the understanding of the anthropogenic earthquakes.

4.2 Experimental material and methods

4.2.1 Sample description and preparation

Bukit Timah granite is used in this study. The mineralogical, physical, mechanical and hydraulic properties of this granite are listed in Table A-1. A sawcut fracture (Figure 4-1a) and a natural fracture (Figure 4-1b) were prepared in two cylindrical core samples without visible cracks on the sample surfaces. The sawcut fracture was used to simulate the tensile fracture with well-matched asperities produced by hydraulic fracturing (Yushi et al., 2016), and the natural fracture with mismatched asperities is ubiquitous in rocks and formed in a variety of geological processes (Zhao, 1997). The sawcut fracture was produced by cutting a core sample of 50 mm diameter and 100 mm height at 30° to the core axis and polishing the fracture surfaces using fine sandpaper with 25.6 μm particle size. The natural fracture inclined 27° to the core axis was obtained by compressing a core sample of 50 mm diameter and 123.8 mm height containing an unopened fracture until it purely broke along the fracture plane. The sample assembly installed inside the triaxial pressure vessel is shown in Figure 3-11. Two boreholes of 2 mm diameter parallel to the core axis were drilled at both ends of the sample to facilitate fluid flow from the coreholders into the fracture. Two Teflon layers were used to fix the sample on the coreholders and to isolate the sample from the confining oil. Two pieces of filter papers of 0.45 μm pore size and 130 μm thickness were inserted in the interfaces between the sample ends and coreholders to keep sheared-off particles out of the pore lines.

The microstructure of the fracture surfaces was analyzed qualitatively to characterize the shear-induced asperity degradation.

4.2.3 Experimental procedure

Figure 4-2a presents the experimental principle, in which increasing the fluid pressure reduces the effective normal stress and drives the fracture from a critical stress state to an unstable state. Figure 4-2b summarizes the procedures for the three triaxial shear-flow tests on the sawcut fracture SF1, sawcut fracture SF2 and natural fracture. The sawcut fracture was used twice to reduce the impact of material heterogeneity, and the fracture surfaces were controlled by the same sandpaper. For the sawcut fracture SF1, the confining pressure was first cycled between 6 and 21 MPa until negligible AE events were detected to eliminate the sample misalignment, and purged air out of the pore system to fully saturate the fracture. Second, the displacement-driven shear tests were used to obtain the shear strength of the fracture, and the injection-driven shear tests simulated the injection-induced instability of the fracture close to a critical stress state. Taking the first test on the sawcut fracture SF1 (1S1) as an example (Figure 4-2c), under 5 MPa effective normal stress and 1 MPa fluid pressure, the displacement-driven shear test (1S1D) was carried out at an axial displacement rate of 1 $\mu\text{m/s}$ to obtain the shear strength. Subsequently, the normal stress was fixed and the shear stress equivalent to 80% of the shear strength was applied, and it is assumed that the fracture was approaching a critical stress state. After that, the injection-driven shear test (1S1I) was performed by increasing the fluid pressure at an injection rate of 0.002 MPa/s until the fracture slip occurred. Next, another two tests were conducted on the sawcut fracture at the same injection rate under 10 and 20 MPa effective normal stresses. Three tests were also carried out on the sawcut fracture SF2 at different injection rates (i.e., 0.006, 0.018, and 0.054 MPa/s) under 10 MPa effective normal stress.

For the natural fracture, after cycling the confining pressure between 1 and 11 MPa and purging air out of the pore system, three displacement-driven shear tests (N1D-N3D) were carried out under the low effective normal stresses of 1, 2 and 3 MPa to derive the Mohr-Coulomb failure envelope. When the fracture was under a high

effective normal stress (i.e., 10 MPa), the shear strength was assessed from the failure envelope without performing additional displacement-driven shear tests to minimize the potential damage to fracture asperities. The shear stress equivalent to 80% of the shear strength was then applied and three injection-driven shear tests (N1I-N3I) were finally conducted at different injection rates (i.e., 0.0004, 0.002, and 0.01 MPa/s).

(a)

Sawcut fracture SF1

Sawcut fracture SF2

Natural fracture

(b)

(c)

Figure 4-2 Experimental principle and procedure. (a) Fluid pressure increase (ΔP) solely drives the point representing a rock fracture subjected to a critical shear stress (τ_c) towards the Mohr-Coulomb failure envelope. The critical shear stress is assumed as 80% of the shear strength (τ_s). The normal stress (σ_n) and shear stress (τ) on the fracture are fixed by adjusting the axial stress (σ_1) and confining pressure (σ_3) during fluid injection. Friction angle and cohesion are denoted as ϕ and c , respectively. (b) Procedures for the triaxial shear-flow tests on the sawcut (SF1 and SF2) and natural fractures. The test condition of corresponding Test No. is given in Table 4-1. (c) Triaxial shear-flow test is composed of displacement-driven and injection-driven shear tests. The shear strength is determined in the displacement-driven shear test, and fluid injection is performed to induce fracture slip in the injection-driven shear test.

4.3 Experimental Results

4.3.1 Fluid pressure heterogeneity

Figure 4-3 shows the Mohr-Coulomb failure envelopes for the sawcut and natural fractures. The failure envelope for the sawcut fracture SF1 is constructed by fitting the shear strengths obtained from the displacement-driven shear tests under the effective normal stresses of 5, 10 and 20 MPa (Figure 4-3a), and for the sawcut fracture SF2 is constructed by fitting the shear strengths under 10 MPa effective normal stress (Figure 4-3b). The cohesion and shear hardening effect were

neglected (Tembe et al., 2010), and the same friction coefficient is derived as 0.64. For the natural fracture, the shear strengths obtained from the displacement-driven shear tests under the effective normal stresses of 1, 2 and 3 MPa are used to construct the failure envelope (Figure 4-3c), which is used to estimate the shear strength under 10 MPa effective normal stress. In this case, the friction coefficient is 0.77, and the cohesion is 0.12 MPa.

As shown in Figure 4-3a-b, the stress state of the sawcut fracture before the injection-driven shear test is denoted as a solid red circle, which lies below the failure envelope. When the fluid is injected into the fracture at a constant injection rate, the effective normal stress continuously reduces until an open red circle representing the stress state at the onset of fracture instability overshoots the failure envelope. The fluid pressure measured from the injection boreholes is thus larger than that predicted by the failure criterion. The heterogeneous fluid pressure distribution with a high fluid pressure in the vicinity of the boreholes has been observed in other experimental and field studies (Cappa et al., 2019; Guglielmi, Cappa, et al., 2015; Rutter and Hackston, 2017). The fluid overpressure ratio is defined as the ratio of fluid pressure measured from the injection boreholes at the onset of injection-driven fracture instability to that predicted by the failure criterion (Table 4-1). The fluid overpressure ratio is unity when the fluid pressure distribution is uniform on the fracture, and it increases with a more localized fluid pressurization. Figure 4-3a demonstrates that the difference between the measured and predicted fluid pressures increases with higher effective normal stress. This is presumably because that a high effective normal stress squeezes the fracture aperture and reduces the fracture permeability, amplifying the fluid overpressure ratio. The deviation of the measured fluid pressure from the predicted fluid pressure increases with higher injection rate (Figure 4-3b), also resulting in the increase in the fluid overpressure ratio. The pressurized fluid can locally induce the nucleation of fracture slip and eventually cause fracture instability (Garagash and Germanovich, 2012; Passelegue et al., 2018).

For the natural fracture, the open red circle slightly deviates from the failure envelope (Figure 4-3c). A mismatched and unsealed fracture provides various fluid flow pathways and impedes localized pressure build-up (Wu et al., 2017). The deviation between the measured and predicted fluid pressures at the onset of fracture instability slightly increases with higher injection rate. Furthermore, the fluid overpressure ratio ranges from 0.99-1.18, which means a near-uniform increase of fluid pressure. It also indicates that fluid pressure heterogeneity plays an insignificant role in the instability of the natural fracture. The test setup currently cannot visualize fluid pressure gradient along the fracture, and thus the injection-induced instability of the entire fracture is the focus of this study.

(a)

(b)

(c)

Figure 4-3 Mohr-Coulomb failure envelopes obtained by fitting the shear strengths (open black squares) from the displacement-driven shear tests, and the stress states of sawcut fractures (a) SF1 and (b) SF2, and (c) natural fracture before the fluid injection (solid red circle) and at the onset of injection-driven fracture instability (open red circle).

Table 4-1 Summary of test procedures and results. A rock fracture is subjected to a normal stress (σ_n) and a fluid pressure (P) in the displacement-driven shear test, and a normal stress, a critical shear stress, an initial fluid pressure (P_i) and an injection rate (P_r) in the injection-driven shear test.

Test No.	Test condition	Failure mode	Residual shear strength (MPa)	Test No.	Test condition	Critical shear stress (MPa)	Failure mode	Fluid overpressure ratio	Shear stress drop (MPa)	Slip displacement (μm)	Peak slip rate (mm/s)	Peak acceleration (mm/s ²)
Sawcut fracture (SF1)												
Displacement-driven shear test				Injection-driven shear test								
1S1D	$\sigma_n=6$ MPa, $P=1$ MPa	Stable Sliding	2.77	1S1I	$\sigma_n=6$ MPa, $P_i=1$ MPa, $P_r=0.002$ MPa/s	2.74	Dynamic slip	1.70	0.40	26.64	0.44	43.32
1S2D	$\sigma_n=11$ MPa, $P=1$ MPa	Stick-Slip	5.06	1S2I	$\sigma_n=11$ MPa, $P_i=1$ MPa, $P_r=0.002$ MPa/s	4.98	Dynamic slip	1.56	0.64	39.96	0.76	66.25
1S3D	$\sigma_n=21$ MPa, $P=1$ MPa	Stick-Slip	10.08	1S3I	$\sigma_n=21$ MPa, $P_i=1$ MPa, $P_r=0.002$ MPa/s	9.62	Dynamic slip	3.10	1.15	53.88	1.87	113.73
Sawcut fracture (SF2)												
Displacement-driven shear test				Injection-driven shear test								
2S1D	$\sigma_n=11$ MPa, $P=1$ MPa	Stable Sliding	6.03	2S1I	$\sigma_n=11$ MPa, $P_i=1$ MPa, $P_r=0.006$ MPa/s	4.89	Dynamic slip	1.62	0.84	126.42	1.11	113.25
2S2D	$\sigma_n=11$ MPa, $P=1$ MPa	Stable Sliding	6.24	2S2I	$\sigma_n=11$ MPa, $P_i=1$ MPa, $P_r=0.018$ MPa/s	5.11	Dynamic slip	1.80	0.99	250.29	1.16	108.29
2S3D	$\sigma_n=11$ MPa, $P=1$ MPa	Stable Sliding	6.84	2S3I	$\sigma_n=11$ MPa, $P_i=1$ MPa, $P_r=0.054$ MPa/s	5.57	Dynamic slip	2.19	1.30	319.72	1.07	60.11
Natural fracture												
Displacement-driven shear test				Injection-driven shear test								
N1D	$\sigma_n=1$ MPa, $P=0$ MPa	Stable Sliding	0.87	N1I	$\sigma_n=11$ MPa, $P_i=1$ MPa, $P_r=0.0004$ MPa/s	6.41	Quasi-dynamic sliding	0.99	-	-	0.004	0.050
N2D	$\sigma_n=2$ MPa, $P=0$ MPa	Stable Sliding	1.71	N2I	$\sigma_n=11$ MPa, $P_i=1$ MPa, $P_r=0.002$ MPa/s	6.41	Quasi-dynamic sliding	1.15	-	-	0.011	0.008
N3D	$\sigma_n=3$ MPa, $P=0$ MPa	Stable Sliding	2.41	N3I	$\sigma_n=11$ MPa, $P_i=1$ MPa, $P_r=0.01$ MPa/s	6.41	Quasi-dynamic sliding	1.27	-	-	0.022	0.012

4.3.2 Injection-induced fracture instability

Figure 4-4 illustrates the injection-induced instability of the sawcut (e.g., 1S1I) and natural (e.g., N3I) fractures. The results of the injection-driven shear test reveal that the sawcut fracture is initially stable, followed by a shear stress drop and a shear displacement jump (Figure 4-4a). The injection-driven dynamic slip resembles the displacement-driven coseismic slip (Karner and Marone, 2000), in terms of an abrupt stress drop and a sudden displacement jump. Table 4-1 presents that the peak slip rate ranges from 0.44 to 1.87 mm/s, which falls in the transition range of the peak slip rate between slow and regular earthquakes (Tinti et al., 2016). The peak acceleration range is 43.32-113.73 mm/s². The sawcut fracture eventually arrests, accompanied by a shear stress recovery to the initial level. The fluctuation of data points is likely due to noise measurement.

The natural fracture exhibits quasi-dynamic sliding in the injection-driven shear test (Figure 4-4b). The shear stress keeps almost constant, whereas the shear displacement increases exponentially. The peak slip rate is in a range of 0.004-0.022 mm/s, which lies within the range of the peak slip rate of slow slip events (Leeman et al., 2016). The peak acceleration is 0.008-0.05 mm/s², which is three orders of magnitude smaller than that in the dynamic slip cases. The fluid injection is terminated manually after fully observing the fracture behaviors induced by fluid injection at shear displacements of 0.25, 1.34 and 1.34 mm under injection rates of 0.0004 (N1I), 0.002 (N2I) and 0.01 MPa/s (N3I), respectively, and the subsequent behaviors are not in the scope of this study. The term quasi-dynamic sliding refers to the slip mode of a fracture transitioning from stable to unstable behavior (Leeman et al., 2016; Tinti et al., 2016; Scuderi et al., 2016). For the natural fracture, the injection-induced instability shows no obvious shear stress drop until the termination of fluid injection, but the slip is accelerating until the maximum acceleration appears. It cannot be termed as stable sliding due to the slip acceleration, or as unstable slip due to the negligible shear stress drop. Therefore, quasi-dynamic sliding is used to describe the injection-induced slip of the natural fracture.

(a)

(b)

Figure 4-4 Injection-induced instability of (a) sawcut fracture and (b) natural fracture. The dynamic slip of the sawcut fracture is accompanied by an abrupt stress drop and a sudden displacement jump, while the quasi-dynamic sliding of the natural fracture exhibits an exponential displacement growth and a small acceleration.

4.3.3 Acoustic emission characteristics

Acoustic emission reflects the rapid release of strain energy during fracture instability (Lockner, 1993; Lockner et al., 1991). The results of the injection-driven

shear test (e.g., 1S1I) indicate that the sawcut fracture is activated with a sudden shear displacement increase and a remarkable AE count rate (Figure 4-5a). The violent energy release occurs simultaneously with an abrupt stress drop shown in Figure 4-4a. Nearly no released energy is detected before and after the dynamic slip, showing the challenge of inspecting the pre- and post-processes of asperity damage to the sawcut fracture. The rapid release of strain energy is commonly observed in the triaxial shear tests on dry sawcut fractures, in which highly packed asperities fail simultaneously (McLaskey and Lockner, 2014).

Figure 4-5b depicts that the natural fracture releases intermittent AE energy before the quasi-dynamic sliding in the injection-driven shear test (e.g., N3I). The highest AE count rate appears at the onset of quasi-dynamic sliding, followed by low AE count rates occasionally emerging in the shear process (Figure 4-4b). The cumulative release energy of the natural fracture is about two orders of magnitude lower than that of the sawcut fracture, indicating that the energy release is dependent on fracture surface roughness. The energy release pattern during the injection-induced sliding of the natural fracture is similar to that observed in the direct shear tests on dry natural fractures, in which the progressive failure occurs on irregularly distributed asperities (Moradian et al., 2010).

(b)

Figure 4-5 Acoustic emission characteristics of (a) sawcut fracture and (b) natural fracture. The sawcut fracture exhibits violent energy release, while the natural fracture shows intermittent energy release.

4.3.4 Fracture surface evolution

Figure 4-6 shows the meso-scale surface roughness of the sawcut fracture SF1 and natural fracture before and after the triaxial shear-flow tests, respectively. Both the sawcut and natural fractures experience the reductions of asperity elevation, which are illustrated as sheared-off layers. The sheared-off thickness of the sawcut fracture follows the Gaussian distribution with a mean of 0.125 mm and a standard deviation of 0.086 mm, while that of the natural fracture shows the bimodal Gaussian distribution with two peaks at 0.258 mm and 1.242 mm thickness. The thickness distributions of the sheared-off layers indicate complete and even wear on the sawcut fracture (Wu et al., 2017) and incomplete and uneven wear on the natural fracture with two high elevation asperities being remarkably abraded (Ciavarella, 2016; Selvadurai and Glaser, 2017). The negative sheared-off thickness is due to the calculation error associated with unscannable points on the irregular surfaces.

(a)

(b)

Figure 4-6 Surface roughness before and after the triaxial shear-flow tests on (a) sawcut fracture and (b) natural fracture, as well as the sheared-off layers derived from asperity elevation changes and corresponding frequency distributions of sheared-off thickness.

(a)

(b)

Figure 4-7 Microstructure of sawcut fracture (a) before (magnitude 5000x) and (b) after (magnitude 2000x) triaxial shear-flow tests. The white arrow indicates the shear direction. Randomly distributed asperities are abraded in strips along the shear direction.

The microstructure of the sawcut fracture qualitatively demonstrates the shear-induced evolution of asperity population (Figure 4-7). Micrometer-sized asperities on the sawcut fracture are initially distributed in a random manner (Figure 4-7a). After the tests, the fracture surface is discretized into several strips along the shear direction (Figure 4-7b). Shallow trenches and abraded asperities indicate that asperity contacts control the fracture instability in some strips, while unmodified or slightly modified asperities imply that the other strips experience insignificant wear.

4.4 Discussion

4.4.1 Mechanism of injection-induced fracture instability

The data show that the injection-induced fracture instability is influenced by fracture surface roughness. The sawcut and natural fractures exhibit dynamic slip and quasi-dynamic sliding, respectively. Figure 4-8a demonstrates that the friction coefficient of the sawcut fracture abruptly drops from the static friction coefficient to the dynamic friction coefficient, and slowly reverts to the static friction coefficient (e.g., 1S1I). Based on the slip weakening friction law, the dynamic weakening of the fracture occurs when the stiffness of the test system is less than

the slip weakening rate of the fracture (Rice, 1983). The stiffness of the test system is determined as the slope of the linear portion of the shear stress versus load point displacement curve obtained from the displacement-driven shear test. The slip weakening rate of the fracture is estimated as the slope of the linear portion of the same curve obtained from the injection-driven shear test. The test system stiffness (17.2 MPa/mm from 1S1D) is much smaller than the slip weakening rate (169.6 MPa/mm from 1S1I) (Figure 4-8b). In other words, the fracture weakening rate far exceeds the system unloading rate, leading to the fracture instability (Leeman et al., 2016). After the dynamic slip, the friction coefficient recovers owing to the re-formation and re-strengthening of asperity contacts (Marone, 1998a; Tesei et al., 2012). For the natural fracture (e.g., N3I), the friction coefficient slightly increases in the shear process (Figure 4-8a). The frictional strength, determined as the product of the friction coefficient and effective normal stress, obviously reduces with decreasing effective normal stress during fluid injection. The quasi-dynamic sliding of the fracture is not resulted from the low stiffness of the test system, because the maximum weakening rate of the fracture (39.3 MPa/mm) is far less than the test system stiffness (63.9 MPa/mm from the linear portion of shear stress versus load point displacement curve under 10 MPa effective normal stress). The increasing stress imbalance between the fixed shear stress and decreasing frictional strength eventually induces the fracture instability, which thus exhibits slow but accelerated quasi-dynamic sliding (Figure 4-8b).

Several physical processes control the injection-induced fracture instability, such as thermal pressurization and stress imbalance. The dynamic slip of the sawcut fracture is strongly influenced by friction loss and thermal pressurization and highly dependent on initial effective normal stress. On the one hand, fracture asperities are sheared off uniformly (Figure 4-6a), accompanied by a high slip rate (Figure 4-4a) and intensive energy release (Figure 4-5a), indicating friction loss in the dynamic process. On the other hand, no melted structure on the sawcut fracture is observed after the triaxial shear-flow tests (Figure 4-7b), which is primarily due to the low effective normal stress, the presence of pore fluid and slow slip rate in the tests (Acosta et al., 2018). This observation suggests flash heating is not a candidate mechanism in the test. However, fluid pressure can be amplified due to thermal

pressurization, leading to the reduction of shear resistance (Acosta et al., 2018; Andrews, 2002; Rice, 2006). Under higher initial effective normal stress, both the friction loss and thermal pressurization result in the decrease in dynamic friction coefficient and the increase in peak slip rate (Figure 4-9a). The quasi-dynamic sliding of the natural fracture largely relies on stress imbalance. The friction coefficient and peak slip rate from the three injection-driven shear tests under different injection rates are taken at a shear displacement of 0.25 mm (Figure 4-9b). Increasing injection rate induces the slight increase in friction coefficient and the remarkable rise of peak slip rate. The slight increase of friction coefficient is presumably due to the overestimate of shear strength during the quasi-dynamics sliding. The degree of overestimate increases with increasing stress imbalance under higher injection rate. The intrinsic velocity-strengthening behavior of rough fractures may also contribute to the increase of friction coefficient (Fang, Elsworth, Ishibashi, et al., 2018). The remarkable rise of peak slip rate is probably due to amplified acceleration (Figure 4-4b) caused by the enhanced stress imbalance between the fixed shear stress and decreasing frictional strength (Figure 4-8b).

(a)

(b)

Figure 4-8 Mechanism of injection-induced instability of sawcut and natural fractures. (a) Friction coefficient as a function of load point displacement, showing the dynamic weakening of the sawcut fracture and the frictional strengthening of the natural fracture. (b) Comparison between the stiffness of the test system and the slip weakening rate of the sawcut fracture, and the stress imbalance between the fixed shear stress and decreasing frictional strength of the natural fracture.

(a)

(b)

Figure 4-9 Friction coefficient and peak slip rate as a function of (a) initial effective normal stress on sawcut fracture and (b) fluid injection rate for natural fracture.

4.4.2 Comparison to natural earthquakes

In order to compare the six dynamic slip cases on the sawcut fracture with natural earthquakes in terms of stress drop and slip displacement, the modulus of rigidity (G) is needed to be determined, which is not experimentally accessible and can be considered as the combined rigidity of the test system and the fracture. The combined rigidity (G) is estimated based on the relationships among moment magnitude (M_w) and seismic moment (M_0), and fracture area (A) in Eqs. (2-26)-(2-28) (Ji et al., 2019; Kanamori and Brodsky, 2001; Wells and Coppersmith, 1994)

Particularly, for the sawcut fracture with major and minor axes of 100 mm and 50 mm, respectively, the fracture area is $3.9 \times 10^{-9} \text{ km}^2$, and the expected moment magnitude is -4.17 based on the empirical relationship between the moment magnitude and fracture area (Eq. (2-28)). In the six dynamic slip cases with known slip displacement (Table 4-1), the moment magnitudes are calculated using Eqs. (2-26) and (2-27) with an unknown combined rigidity. The expected moment magnitude -4.17 then used to constrain the combined rigidity by the least squares method and obtain a best-fit value of 1.9 GPa. As shown in Figure 4-10a, with this

rigidity, the six dynamic slip cases fall within the range constrained by three times standard deviation uncertainty from the empirical relationship defined by Eq. (2-28) (Wells and Coppersmith, 1994; Yoon et al., 2017). To further validate the derived rigidity, the six dynamic slip cases are plotted on the figure showing the relationship between seismic moment and fracture size (Figure 4-10b), in which the stress drop, $\Delta\tau$, and slip displacement, d , are expressed respectively in Eqs. (2-29) and (2-30), respectively. The fracture size, which is determined as the average length of the major and minor axes of the oval-shaped fracture and equal to 0.075 m.

The stress drop and slip displacement of natural earthquakes increase with larger fracture size (Figure 4-10b). For the injection-driven dynamic slip on the sawcut fracture, the stress drop falls within a range from 0.3 to 2.0 MPa, and the slip displacement ranges from 20 to 300 μm . The results agree well with the stress drop and slip displacement directly measured from the tests (Table 4-1), indicating that the derived rigidity is reasonable, and the stress drops of injection-induced and natural earthquakes are comparable and thus indistinguishable (Huang et al., 2017).

(a)

(b)

Figure 4-10 Comparison to natural earthquakes through determination and validation of the combined rigidity (G) of the test system and the sawcut fracture. (a) Moment magnitudes (M_w) as a function of fracture size (A). A best-fit value of the combined rigidity is obtained based on the least squares method. s is the standard deviation of the empirical relationship. (b) Seismic moment (M_0) as a function of fracture size. The combined rigidity is validated by comparing the estimated ranges of stress drop and slip displacement in this figure and the test results listed in Table 4-1.

4.4.3 Injection-induced moment release

The comprehensive investigation of injection-induced fracture instability can improve the ability to predict the injection-induced moment release. Several analytical models have been established between the moment release and injected volume (Galis et al., 2017; McGarr, 2014; McGarr and Barbour, 2018). McGarr (2014) assumes that the injection-induced earthquakes are confined within the stimulated reservoirs, and suggests that the total moment release (ΣM_0), induced by fluid injection has an upper bound, which can be obtained based on the modulus of rigidity (G), and the total volume of injected fluid (ΔV)

$$\Sigma M_0 \leq 2G\Delta V \quad (4-1)$$

If the Gutenberg-Richter exponent (b) is assumed as unity, the maximum moment release is half of the total moment release (McGarr, 2014)

$$M_0(\text{max})=G\Delta V \quad (4-2)$$

In Eq. (4-2), the fracture is stressed under a critical shear stress equal to the shear stress after 50% of the shear stress drop from the shear strength. However, for the sawcut fracture, the critical shear stress (80% of the shear strength) used in the injection-driven shear tests is approximately equal to the residual shear strength (i.e., the shear stress after 100% of the shear stress drop) obtained in the displacement-driven shear tests (Table 4-1). The maximum moment release for the sawcut fracture is thus estimated as

$$M_0(\text{max}) = \frac{1}{2} G\Delta V \quad (4-3)$$

Although nearly no shear stress drop appears in the injection-driven shear tests on the natural fracture, the total moment release due to aseismic deformation can also be evaluated by Eq. (4-1) (McGarr and Barbour, 2018).

Galis et al. (2017) address the occurrence of the injection-induced earthquakes beyond the stimulated reservoirs, and distinguish self-arrested from runaway ruptures. The maximum arrested moment is estimated as

$$M_0(\text{max-arr})=\gamma \cdot \Delta V^{3/2} \quad (4-4)$$

where γ is the reservoir parameter determined as

$$\gamma = \frac{0.4255}{\sqrt{\Delta\tau}} \left(\frac{\kappa\mu_d}{h} \right)^{3/2} \quad (4-5)$$

where κ is the bulk modulus of reservoir rocks; μ_d is the dynamic friction coefficient; $\Delta\tau$ is the shear stress drop; and h is the reservoir thickness.

(a)

(b)

Figure 4-11 Maximum moment release as a function of injected volume. (a) Two fluid injection cases in the injection-driven shear tests on the sawcut fracture SF2 exceed both the maximum moment release (red solid line), $M_0(\max)$, and the maximum arrested moment (blue solid line), $M_0(\max\text{-arr})$. (b) Time-dependent total moment release and its peak value for the natural fracture are below the upper bound (red dashed line), ΣM_0 .

In this study, the maximum moment release for the sawcut fracture predicted by McGarr (2014) is calculated by Eq. (4-3) using the combined rigidity of 1.9 GPa. The maximum arrested moment can be estimated using Eq. (4-4) based on the bulk modulus of the reservoir rock (1.3 GPa), dynamic friction coefficient (0.5), shear stress drop (1 MPa) and reservoir thickness (0.05 m). The bulk modulus is estimated as 2/3 of the combined rigidity (Landau et al., 1960). The dynamic friction coefficient and shear stress drop are assessed from Figure 4-9 and Table 4-1, respectively. The reservoir thickness is the ratio of the sample volume to fracture area (Galis et al., 2017). The reservoir parameter is thus calculated as 0.6×10^{12} . As shown in Figure 4-11a, the upper bounds obtained from Eqs. (4-3) and (4-4) can cap the maximum moment release of all the cases from the injection-driven shear tests on the sawcut fracture SF1. However, two out of three cases on the sawcut fracture SF2 exceed the upper bounds. The injection rates in the two cases are much higher than those in the other cases, indicating that a high fluid overpressure ratio likely induces the propagation of fracture slip beyond the pressurized area (Garagash and Germanovich, 2012; Passelegue et al., 2018).

For the natural fracture, the moment release during the quasi-dynamic sliding is aseismic (Figure 4-5b). The same combined rigidity of 1.9 GPa is assumed, and the fracture area is $4.3 \times 10^{-9} \text{ km}^2$. Because no obvious shear stress drop is observed in the injection-driven shear tests, the time-dependent shear displacement is input (Figure 4-4b) to calculate the time history of the total moment release using Eq. (4-1). Figure 4-11b exhibits that the total moment release slightly increases at the early stage of fluid injection, followed by a dramatic jump approaching the upper bound. The turning point of the moment release increase can be observed in the three cases with different injection rates. This observation indicates that the turning point of the total moment release (or shear displacement) could be used as a precursor to forecast the instability of natural fractures during fluid injection. In the geothermal field, if displacement sensors or geodetic techniques can detect the dramatic change of fracture movement, one may use the turning point as a reference to optimize the fluid injection strategy. Monitoring fracture slip to assess the fracture instability during fluid injection is also suggested by Scuderi and Collettini

(2018). Also, the aseismic slip may promote fracture instability near the injection borehole, resulting in unstable fracture slip outside the pressurized area (Eyre et al., 2019; Guglielmi, Cappa, et al., 2015).

4.5 Implications for injection-induced seismicity

Understanding the mechanism of injection-induced seismicity is essential to predict the likelihood of the anthropogenic earthquakes due to fluid injection. In the triaxial shear-flow tests, the injection-induced responses of sawcut and natural fractures are investigated, which represent the hydraulically induced and ancient fractures in geothermal reservoirs, respectively. The results show that the fracture instability is strongly dependent on fluid pressure heterogeneity and fracture surface heterogeneity.

One of the mysteries behind injection-induced seismicity is about how fluid pressure heterogeneity influences fracture instability. Recent studies reveal that the relationship between the size of pressurized area and the size of slip area relies on the fluid overpressure ratio (Bhattacharya and Viesca, 2019; Cappa et al., 2019; Galis et al., 2017; Guglielmi, Cappa, et al., 2015). When the fluid pressure is homogeneously distributed on the fracture, both the pressurized area and slip area are roughly equal to the fracture area. The fracture instability is mainly due to the near-uniform reduction of the effective normal stress. When the fluid overpressure ratio increases, the slip area exceeds the pressurized area. The fluid pressure amplifies near the injection boreholes, and induces the fracture slip propagating in the unpressurized area (Garagash and Germanovich, 2012; Jia et al., 2020; Passelegue et al., 2018). The occurrence of the Pohang earthquake is attributed to fluid injection into a highly heterogeneous fault (Kim et al., 2018). The fault slip induced by localized fluid pressure propagates along the fault controlled by the tectonic stresses, and finally trigger the huge earthquake releasing seismic energy from the entire fault. Although the size of the tests is too small to fully reproduce the initiation and propagation of fracture slip, the results clearly show that the fluid overpressure ratio increases with larger normal stress and higher injection rate (Figure 4-3a and b). Additionally, Figure 4-12 exhibits the maximum moment release generally increases with higher fluid overpressure ratio. The fluid injection

through one borehole (detailed in Chapter 5) induces a higher growth rate of the maximum moment release than that through two boreholes in this study. The comparison indicates that the increase in flow pathways between the injection borehole and the fracture could promote uniform fluid pressure distribution and maintain the fracture slip within the pressurized area.

Figure 4-12 Maximum moment release as a function of fluid overpressure ratio. Data points of Tests 1-3 are obtained from Table 5-1.

When the instability occurs, the sawcut and natural fractures exhibit distinct patterns of strain energy release due to the spatial variation of fracture surface roughness. The sawcut fracture fails accompanied by the seismic events with strain energy abruptly releasing at the onset of the dynamic slip, while the natural fracture shows the aseismic behaviors with the energy intermittently releasing before and during the quasi-dynamic sliding. The results indicate that the seismic and aseismic events induced by fluid injection are linked to fracture surface heterogeneity. In the acidified water injection test in the Soultz-sous-Forêts geothermal field, scattered and small seismic clusters appear after the injection shut-in. However, before that, the clusters of large seismic events are highly concentrated around the injection boreholes during the fresh water injection (Calò et al., 2014). The transition from seismic to aseismic behavior may be interpreted by the change of fracture surface

roughness. Although the minerals in the granite matrix around the boreholes are dissolved slowly (Portier et al., 2009), the hydrochloric acid solution can rapidly flow into hydraulically induced fractures and effectively roughen the fracture surfaces by removing dissoluble minerals. The acid-etched fractures with rough surfaces mainly show aseismic behaviors, resulting in fewer dynamic slip events detected by seismic sensors.

This study indicates that fluid pressure heterogeneity and fracture surface heterogeneity can be managed to modulate the risk of injection-induced fracture instability. In enhanced geothermal systems, it is critical to control the occurrence of the fracture slip within the stimulated reservoirs and to avoid the fracture slip propagating beyond the reservoirs. This strategy could be realized by maintaining a low fluid overpressure ratio, such as cyclic soft stimulation and chemical fluid injection. Nevertheless, further investigations are needed to appraise the overall performance of the strategy.

4.6 Summary

The triaxial shear-flow tests were carried out to study the mechanism of injection-induced instability of sawcut and natural fractures in granite. Particularly, fractures approaching critical stress states before fluid injection are simulated, and fluid pressure heterogeneity, injection-induced fracture instability, acoustic emission characteristics and fracture surface evolution are carefully examined during the injection. The data show that the injection-induced fracture instability is strongly dependent on fluid pressure heterogeneity and fracture surface heterogeneity. On the one hand, a high fluid overpressure ratio in the sawcut fracture causes fluid pressure amplification near the injection boreholes, leading to that the maximum moment release exceeds the upper bounds predicted by McGarr (2014) and Galis et al. (2017). The instability of the natural fracture is due to the near-uniform reduction of effective normal stress associated with a low fluid overpressure ratio. Fluid pressure heterogeneity strongly influences the fracture instability. On the other hand, the sawcut fracture exhibits dynamic slip with friction loss and thermal pressurization, while the natural fracture experiences quasi-dynamic sliding with stress imbalance between the fixed shear stress and decreasing frictional strength.

Fracture surface heterogeneity controls the seismic and aseismic behaviors of the fractures when the instability occurs. The data indicate that the moment release based on injected volume is predictable in the case with a low fluid overpressure ratio. However, the current models are not suitable for the case with a high fluid overpressure ratio, which may induce the fracture slip beyond the stimulated reservoirs. The total moment release for the natural fracture shows a turning point before approaching the upper bound, which could be used as a reference to optimize the fluid injection strategy.

CHAPTER 5 NUMERICAL SIMULATION OF FLUID PRESSURE HETEROGENEITY ON FLUID-PRESSURIZED FRACTURES

5.1 Introduction

The understanding of injection-induced fracture activation is primarily based on the Mohr-Coulomb (MC) failure criterion and effective stress law (Jaeger and Cook, 2014; Terzaghi, 1923). When the shear stress exceeds the shear strength of a rock fracture, which is the product of the friction coefficient and effective normal stress, the fracture instability occurs due to the reduction of effective normal stress on the fracture. Nevertheless, laboratory and field investigations recently reveal that the fluid pressure required to activate the fracture can largely exceed that predicted by the MC failure criterion. Laboratory tests show that the fluid pressure at fracture failure is consistent with the predicted pressure when the fluid is injected at a slow injection rate into the fracture under a low stress level. However, the predicted pressure is smaller than the fluid pressure required to activate the fracture when the injection rate becomes fast and the stress level is high (Passelegue et al., 2018). After the fracture failure, the fluid pressure required to sustain the injection-driven shear slip reduces with increasing shear displacement due to the permeability enhancement caused by shear dilation (Dang et al., 2019; Rutter and Hackston, 2017; Saadat and Taheri, 2020; Wu et al., 2017; Zhao et al., 2019; Zhao et al., 2018). The deviation of the fluid pressure at fracture failure from the predicted pressure is attributed to the non-uniform distribution of fluid pressure on the fracture (i.e., fluid pressure heterogeneity (Passelegue et al., 2018)). This phenomenon has also been observed in several field cases (Eyre et al., 2019; Grigoli et al., 2018; Kim et al., 2018). For instance, the 2017 M_w 5.5 Pohang earthquake was resulted from the highly localized fluid pressurization, which activated a local fault segment and subsequently destabilized the entire fault after the hydraulic fracturing in a nearby enhanced geothermal system (Grigoli et al., 2018; Kim et al., 2018). In the Duvernay shale formation, fluid overpressure in a fault segment triggered the instability of carbonate-rich fault segments lying ~400 m above, resulting in the 2016 M_w 4.1 earthquake (Eyre et al., 2019).

The objective of this chapter is to investigate the development of fluid pressure heterogeneity in a rock fracture and its influence on injection-induced fracture activation. Injection-driven shear tests on a sawcut fracture close to a critical stress state using a triaxial shear-flow setup are first conducted. The fluid pressure distribution on the fracture using COMSOL Multiphysics software is then recovered. The numerical model is finally used to investigate the relationship between the fluid pressure at fracture failure and the degree of fluid pressure heterogeneity, and further explain the occurrence of the anthropogenic earthquakes due to the fluid injection.

5.2 Experimental study

5.2.1 Experimental method

A series of injection-driven shear tests on a sawcut fracture in Bukit Timah granite was conducted using the triaxial shear-flow setup (Figure 3-11). The mineralogical, physical, mechanical and hydraulic properties of this granite are listed in Table A-1. The fracture was cut from a 50-mm-diameter and 100-mm-height core sample at 30° to the core axis using a diamond saw. The fracture surfaces were then polished using fine sandpaper with 25.6 μm particle size. Two 2-mm-diameter boreholes were drilled parallel to the core axis at the sample ends to distribute pore fluid in the fracture. The tests were carried out using MTS 815 rock mechanics test system combined with VP-12K Vindum dual syringe pumps at ambient temperature (25 °C). Distilled water and silicon oil were used as the pore and confining fluids, respectively. The shear displacement along the fracture was obtained as the difference between the axial displacement and the deformation of granite material and test system divided by the cosine of the fracture inclination angle to the horizontal plane. The shear stress and normal stress on the inclined fracture were servo-controlled by axial stress and confining pressure and can be determined using Eqs. (3-2) and (3-3), respectively.

In the injection-driven shear tests, the axial displacement, axial stress, and confining pressure were recorded at a sampling rate of 100 Hz. The normal stress and fluid pressures were first increased to the target values. The sample was fully saturated

when the fluid pressure and volume between the two pumps became equilibrium. The sample was subsequently loaded at an axial displacement rate of $1 \mu\text{m/s}$ under a constant effective normal stress to obtain the shear strength (Figure 5-1a). The shear stress equivalent to $\sim 80\%$ of the shear strength was then applied to simulate a critically stressed fracture. Finally, the pore fluid was injected at a constant volumetric rate through the bottom borehole to induce fracture slip. The fluid pressures in the top and bottom boreholes were monitored to observe the fluid pressure distribution on the fracture. As listed in Table 5-1, three tests were carried out under different normal stresses (6 and 11 MPa) and injection rates (0.25 and 1.25 mL/min). Note that, the three tests were conducted consecutively and sequentially from Tests 1 to 3 on the same sawcut fracture to eliminate the effect of sample difference on the results.

(a)

(b)

(c)

Figure 5-1 Test procedure and results. Rock fracture is loaded at 1 $\mu\text{m/s}$ axial displacement rate to derive the shear strength (τ_s) (gray background area), and the fracture under a critical shear stress (τ_c) is activated at (a) 0.25 mL/min injection rate under 6 MPa normal stress (Test 1), (b) 0.25 mL/min injection rate under 11 MPa normal stress (Test 2), and (c) 1.25 mL/min injection rate under 11 MPa normal stress (Test 3).

Table 5-1 Test conditions and results.

Test No.	Initial normal stress (MPa)	Initial fluid pressure (MPa)	Critical shear stress relative to shear strength	Injection rate (mL/min)	Injection pressure at failure (MPa)	Monitoring pressure at failure (MPa)	Degree of fluid pressure heterogeneity at failure	Fluid pressure at failure by MC prediction (MPa)
1	6	0.05	75%	0.25	1.67	1.65	0.01	1.50
2	11	0.2	80%	0.25	2.78	1.15	0.59	2.20
3	11	0.4	80%	1.25	2.86	0.13	0.95	2.12

5.2.2 Experimental results

Figure 5-1 shows the normal and shear stresses, confining and fluid pressures, and shear displacement during the three tests. The occurrence of fracture slip due to the fluid injection is accompanied by a sudden shear stress drop and an abrupt shear displacement jump. The fracture is then arrested owing to the reformation and strengthening of asperity junctions (Marone, 1998a, 1998b). When both the normal stress (6 MPa) and injection rate (0.25 mL/min) are low (Test 1, Figure 5-1a), the fluid pressure initially rises in the injection borehole, and a delayed pressure increase is observed in the monitoring borehole. The fracture slip occurs when the fluid pressure in the injection borehole (1.67 MPa) is close to that in the monitoring borehole (1.65 MPa). The linear regression line for the three shear strengths (τ_s) is constructed as the MC failure envelope by neglecting the cohesion, and the friction coefficient of the fracture is 0.72 (Figure 5-2). According to the MC failure envelope obtained from the displacement-driven shear strengths (Figure 5-2), the predicted pressure at fracture failure from the MC failure envelope is 1.5 MPa for Test 1, which is nearly consistent with the fluid pressures measured from two boreholes. The results demonstrate that the distribution of fluid pressure at fracture failure is near uniform on the fracture. Under 11 MPa normal stress, the fluid injection at a rate of 0.25 mL/min induces 2.8 MPa injection pressure and 1.2 MPa monitoring pressure at fracture failure (Test 2, Figure 5-1b). The predicted pressure at fracture failure is 2.2 MPa. When the injection rate increases to 1.25 mL/min under the same normal stress, the injection and monitoring pressures at fracture

failure are 2.86 MPa and 0, respectively (Test 3, Figure 5-1c). The injection pressure at fracture failure is even larger than the predicted pressure (2.12 MPa). The results indicate the non-uniform fluid pressure distribution on the fracture, also known as fluid pressure heterogeneity (Passelegue et al., 2018; Rutter and Hackston, 2017).

Figure 5-2 Mohr-Coulomb failure envelope obtained from displacement-driven shear strengths, and the stress states before fluid injection and at fracture failure calculated from injection and monitoring pressures.

The degree of fluid pressure heterogeneity can be defined as the ratio of the difference between the fluid pressures at fracture failure in the injection and monitoring boreholes to the fluid pressure at fracture failure in the injection borehole. The results also show that the degree of fluid pressure heterogeneity increases with higher normal stress and faster injection rate (Table 5-1). This is primarily because higher normal stress squeezes the fracture and reduces the hydraulic diffusivity of the fracture, while faster injection rate is prone to accumulate the pore fluid around the injection borehole. The reduction in hydraulic diffusivity of the fracture and accumulation of pore fluid near the injection borehole increase the degree of fluid pressure heterogeneity. The MC failure criterion well

predicts the stress state at fracture failure when the normal stress and injection rate are low (e.g., Test 1). However, it underestimates the fluid pressure at fracture failure in the injection borehole and overestimates that in the monitoring borehole when the normal stress and injection rate are high (Figure 5-2), which is also addressed by previous results (e.g., Passelegue et al. (2018) and Rutter and Hackston (2017)). These observations also demonstrate the reliability of the test results. The tests can only observe the localized fluid pressures in the injection and monitoring boreholes. To better understand the effect of fluid pressure heterogeneity on injection-induced fracture instability, numerical simulation is used to recover the fluid pressure distribution on the entire fracture.

5.3 Numerical simulation

5.3.1 Numerical model

In the injection-driven shear tests, the granite matrix does not affect the fluid pressure distribution on the fracture because of its extremely low permeability compared to that of the pre-existing fracture (Passelegue et al., 2018; Wu et al., 2017; Ye and Ghassemi, 2018). Furthermore, the fracture inclination angle (60°) ensures that the slip can occur easily along the fracture without fracturing the granite matrix (Passelegue et al., 2018; Ye and Ghassemi, 2018). Therefore, the effect of granite matrix was not considered in the numerical model.

Fluid injection into an ellipse-shaped fracture with the minor and major axes of 50 mm and 100 mm, respectively, was modelled using COMSOL Multiphysics software (Figure 5-3a). Two 2-mm-diameter boreholes were introduced 5 mm away from the vertices on the major axis. As shown in Figure 5-3b, a free triangular mesh was used with the maximum and minimum element sizes of 2 and 0.0075 mm, the maximum element growth rate, curvature factor and resolution of narrow regions of 1.2, 0.25 and 1, respectively. The mesh dimensions enable us to obtain the high-resolution fluid pressure distribution on the fracture according to the mesh sensitivity analysis. The effect of gravity was neglected, and the zero-flux boundary condition is considered. The time-dependent fluid pressure at a point (x, y) on the fracture was derived as (Shapiro et al., 1997),

$$\frac{1}{D} \frac{\partial P}{\partial t} = \frac{\partial^2 P}{\partial x^2} + \frac{\partial^2 P}{\partial y^2} \quad (5-1)$$

where D is the hydraulic diffusivity of the fracture, P is the injection pressure, and t is the elapsed time.

Figure 5-3 Numerical model setup, (a) geometry and (b) mesh of ellipse-shaped fracture.

To calibrate the numerical model, the injection pressure measured from the experimental study was used as the input data, and the monitoring pressures obtained from the numerical and experimental studies were compared. In the meantime, the hydraulic diffusivity of the fracture was adjusted to ensure that the monitoring pressure in the numerical study matched that in the experimental study. When the degree of fluid pressure heterogeneity was low (Figure 5-4a-b), the monitoring pressure increased at two different growth rates. Thus, two hydraulic diffusivity values (i.e., $D1$ and $D2$) were used to fit the curve sections and two smoothing parameters (i.e., transition location and period) were used to connect the sections (Table 5-2). For a high degree of fluid pressure heterogeneity (Figure 5-4c), one hydraulic diffusivity value (i.e., $D1$) was used, because the monitoring pressure kept almost constant. Figure 5-4 exhibits that the monitoring pressure obtained from the numerical study is in good agreement with that from the experimental study.

Table 5-2 Numerical calibration of hydraulic diffusivity and smoothing parameters.

Parameter	Test 1	Test 2	Test 3
Hydraulic diffusivity $D1$ (m ² /s)	5.0×10^{-6}	1.8×10^{-6}	1.8×10^{-6}
Hydraulic diffusivity $D2$ (m ² /s)	1.0×10^{-3}	1.7×10^{-5}	-
Transition location with respect to time (s)	1000	750	-
Transition period (s)	400	200	-

(a)

(b)

(c)

Figure 5-4 Numerical model calibration based on fluid pressures measured from (a) Test 1, (b) Test 2, and (c) Test 3. The blue dashed line indicates the fluid pressure at fracture failure predicted by MC failure envelope.

5.3.2 Numerical results

Figure 5-5 illustrates the spatial and temporal evolution of fluid pressure along the major axis of the fracture during the fluid injection. When the normal stress is 6 MPa and the injection rate is 0.25 mL/min, the fluid pressure slightly decreases from the injection borehole. At the fracture failure (1014 s), the fluid pressure is well predicted by the MC failure criterion (Figure 5-5a) and distributed near-uniformly (Figure 5-6a). When the normal stress increases to 11 MPa, the fluid injection at the same rate induces a remarkable increase in fluid pressure around the injection borehole (Figure 5-5b). The fluid pressure at fracture failure (905 s) locally exceeds that predicted by the MC failure criterion (Figure 5-6b), implying that the fracture failure likely initiates near the injection borehole. When the injection rate increases to 1.25 mL/min under 11 MPa normal stress, the fluid pressure rapidly induces the fracture failure at 6 s (Figure 5-5c), because it sharply jumps in the area very close to the injection borehole (Figure 5-6c). The results clearly indicate that the fracture slip can be activated in the area with highly pressurized fluid and may extend to trigger the failure of the other area without pressurized fluid.

(a)

(b)

(c)

Figure 5-5 Spatial and temporal evolution of fluid pressure along the major axis of the fracture during fluid injection, (a) at 0.25 mL/min injection rate under 6 MPa normal stress, (b) at 0.25 mL/min injection rate under 11 MPa normal stress, and (c) at 1.25 mL/min injection rate under 11 MPa normal stress. The red and blue circles represent the positions of the injection and monitoring boreholes, respectively. The blue dashed line indicates the fluid pressure at fracture failure predicted by MC failure criterion.

Figure 5-6 Fluid pressure distribution at fracture failure, (a) 0.25 mL/min injection rate under 6 MPa normal stress, (b) 0.25 mL/min injection rate under 11 MPa normal stress, and (c) 1.25 mL/min injection rate under 11 MPa normal stress.

Figure 5-7 depicts that the difference between the fluid pressures in the injection and monitoring boreholes evolves in various manners during the fluid injection, depending on normal stress and injection rate. The pressurized area, which is unlike the controllable normal stress and injection rate and passively induced by the fluid injection, mirrors the degree of fluid pressure heterogeneity. The pressurized area and injection time are normalized with respect to fracture area and injection duration, respectively, to compare the developments of pressurized area in different injection-driven shear tests. When both the normal stress and injection rate are low (e.g., Test 1), the difference between the injection and monitoring pressures is small, and the pressurized area suddenly reaches the fracture area at the fracture failure. When the normal stress and injection rate become high (e.g., Tests 2 and 3), the pressure difference is large. The pressurized area is developed before the fracture failure but much smaller than the fracture area. Hence, the high degree of fluid pressure heterogeneity is associated with small-sized and early-developed pressurized area, and induces the initiation of fracture slip. The fracture slip propagates beyond the pressurized area, and finally results in the failure of the entire fracture (Cappa et al., 2018; Cappa et al., 2019; Passelegue et al., 2018).

Figure 5-7 Difference of fluid pressures in injection and monitoring boreholes (dashed lines) and normalized pressurized area (solid lines) as a function of normalized time during fluid injection. The blue dashed line indicates the pressurized area equal to fracture area.

5.4 Discussion

The experimental and numerical results of the three cases indicate that the injection-induced fracture activation is strongly related to the fluid pressure distribution on the fracture. To further understand the effect of fluid pressure heterogeneity on injection-induced fracture instability, the COMSOL model is used to explore a general relationship between the fluid pressure at fracture failure and the degree of

fluid pressure heterogeneity. The total force from the pressurized fluid perpendicular to the fracture at time t is calculated as,

$$F = \int_0^A P(t) dA \quad (5-2)$$

where $P(t)$ is the fluid pressure at a point on the fracture at time t , and A is the fracture area.

The total force required to activate a given fracture is independent of fluid pressure distribution on the fracture in a steady-state process (Galis et al., 2017). For the fracture with any degree of fluid pressure heterogeneity, the total force is constant, and can be determined from the special case with uniform fluid pressure (i.e., Test 1) as the product of the fluid pressure at fracture failure and fracture area.

In the numerical simulation, the injection pressure is constant, and the monitoring pressure increases until the fluid pressure on the fracture is uniform (Figure 5-8a). The fracture failure occurs when the total force from the pressurized fluid on the fracture reaches that required to activate the fracture calculated by Eq. (5-2). The influence of hydraulic diffusivity on the degree of fluid pressure heterogeneity is evaluated, because the recovery of monitoring pressure heavily relies on the input value of hydraulic diffusivity (Figure 5-4). The difference between the injection and monitoring pressures is simulated under a normal stress of 10 MPa and a shear stress equivalent to 80% of the shear strength, and choose the extreme values of hydraulic diffusivity shown in Table 5-2 (i.e., 10^{-3} and 10^{-6} m²/s). Figure 5-8b demonstrates that the increase in injection pressure induces larger pressure difference, but the effect of hydraulic diffusivity on the pressure difference is negligible. Thus, a single hydraulic diffusivity value of 10^{-3} m²/s is used in the following simulation. The results show that the total force from the pressurized fluid on the fracture with lower hydraulic diffusivity takes a longer time to reach that required to activate the fracture (Figure 5-8c). Meanwhile, the time to fracture failure decreases when higher injection pressure is applied on the fracture.

(a)

(b)

Figure 5-8 General relationship between fluid pressure at fracture failure and degree of fluid pressure heterogeneity, (a) numerical procedure and failure criterion, (b) difference between fluid pressures in injection and monitoring boreholes as a function of injection pressure, (c) time to fracture failure as a function of injection pressure, and (d) fluid pressure at fracture failure as a function of degree of fluid pressure heterogeneity.

Table 5-3 Numerical conditions in the generalized model.

Case No.	Initial normal stress (MPa)	Critical stress relative to shear strength	Fluid pressure at failure by MC prediction (MPa)	Total force requirement (N)
1	5	80%	1	3900
2	10	70%	3	11700
3	10	80%	2	7800
4	10	90%	1	3900
5	20	80%	4	15600

As shown in Table 5-3, five numerical cases are considered with different combinations of normal stress (e.g., 5, 10, and 20 MPa) and shear stress (e.g., 70%, 80%, and 90% of shear strength), to demonstrate a general relationship between the fluid pressure at fracture failure and the degree of fluid pressure heterogeneity. All the cases show that the fluid pressure at fracture failure increases with higher degree of fluid pressure heterogeneity. The results also show that a higher fluid pressure at fracture failure is resulted from a higher normal stress and a lower shear stress. In other words, the increase in fluid pressure at fracture failure is due to the increase in the total force required to activate the fracture. The results of Cases 1 and 4 coincides as the total forces required to activate the fracture are equal.

This study not only helps understand the mechanism of injection-induced earthquakes, but also inspires us to mitigate the seismic risks during unconventional energy extraction. On the one hand, the experimental and numerical results demonstrate that the fluid pressure at fracture failure is much larger than the fluid pressure predicted by the MC failure criterion due to the high degree of fluid pressure heterogeneity. The fracture slip nucleates in the area with highly pressurized fluid, and extends to trigger the instability of the other areas without pressurized fluid. This observation is similar to that recorded in the field cases. The seismic events occurred in the Pohang geothermal site were induced by the fluid pressure up to 89.2 MPa, which was far larger than the predicted pressure at fault failure (9.8-58.9 MPa) (Park et al., 2017). The seismic events in the Duvernay shale

formation were induced by fluid injection with pressures as high as 60-70 MPa (Eyre et al., 2019), which was also larger than the predicted pressure at fault failure (50 MPa). On the other hand, the likelihood of the earthquake occurrence can be reduced, if the degree of fluid pressure heterogeneity in nearby faults is manageable. The data indicate that the degree of fluid pressure heterogeneity can be decreased through controlling fluid injection (e.g., slow injection rate). Additionally, cyclic fluid injection has been identified as a promising strategy to mitigate the seismic risks (Hofmann et al., 2019). Fluid injection with controlled well-head pressures and flow rates in response to near-real-time seismic monitoring has also been successfully applied to avoid the occurrence of induced earthquakes with magnitudes greater than M_w 2.0 (Kwiatek et al., 2019). Nevertheless, the numerical model simulates the injection-induced activation of a laboratory-scale fracture with idealized surface roughness under simplified stress conditions. This model may not be suitable to consider the injection-induced instability of large-scale faults with spatially varying frictional and hydraulic properties. Hence, a three-dimensional numerical model should be developed with the considerations of injection-induced fracturing in adjacent rocks and coupled thermo-hydro-mechanical fracture behaviors.

5.5 Summary

Experimental and numerical methods are used to observe the evolution of fluid pressure distribution on a rock fracture during the fluid injection and to reveal the relationship between the fluid pressure at fracture failure and the degree of fluid pressure heterogeneity. The experimental results show that the fluid pressure at fracture failure can be predicted by the MC failure criterion when the degree of fluid pressure heterogeneity is low, while the fluid pressure at fracture failure is underestimated when the fluid pressure is highly heterogeneous. The numerical results recover the evolution of fluid pressure distribution on the fracture, and indicate that the failure mode of the fracture is dependent on degree of fluid pressure heterogeneity. This study also demonstrates that the fluid pressure at fracture failure increases with higher degree of fluid pressure heterogeneity, and the fracture slip extends to activate the entire fracture. The understanding of unusual

fluid pressure build-up in critically stressed fractures can offer new injection strategies to mitigate the seismic risks during unconventional energy extraction.

CHAPTER 6 SHEAR STRESS TRANSFER ALONG FRACTURES INDUCED BY FLUID OVERPRESSURIZATION

6.1 Introduction

Many studies have revealed that the most threatening scenario is the injection-induced reactivation of previously unmapped, tectonically controlled faults in vicinity of unconventional reservoirs (Eyre et al., 2019). The current ability to predict the seismic moment release mainly relies on injected volume (Galis et al., 2017; McGarr, 2014; van der Elst et al., 2016). Nevertheless, in some circumstances, blind and potentially seismogenic faults could be reactivated no matter how much fluid is injected into the reservoirs, indicating a need to further improve the understanding on the mechanism of injection-induced seismicity.

One plausible mechanism is the indirect activation of remote, unpressurized fault segments due to shear stress transfer (Elsworth et al., 2016). Shear or Coulomb stress evolving with fault slip has long been used to explain the mechanism of natural earthquakes (Freed, 2005; French et al., 2016). Along the central Calaveras fault in California, shear stress accumulation from the previous earthquakes triggers the subsequent earthquakes on adjacent fault segments (Du and Aydin, 1993). Fault activation associated with shear stress transfer due to fluid overpressurization has been studied in analytical modeling (Garagash and Germanovich, 2012) and laboratory test (Jia et al., 2020; Passelegue et al., 2018), and believed as an essential cause of devastating earthquakes, such as Pohang earthquake in Korea (Lee et al., 2019). However, many field observations still elude explanation. For instance, fluid injection may induce the aseismic slip of nearby, pressurized fracture segment, followed by the seismic slip of remote, unpressurized segment (Guglielmi, Cappa, et al., 2015). The different slip modes can be explained using the rate-and-state friction law (Cappa et al., 2019; Noël et al., 2019; Scuderi et al., 2017), but the transition from the aseismic to seismic slip between the pressurized and unpressurized segments remains enigmatic. Additionally, both the aseismic slip and shear stress transfer fronts could exceed the fluid pressure front (Bhattacharya and Viesca, 2019), but the understanding of the competing propagation among the three

fronts should be extended to reveal the nucleation and propagation of the seismic slip.

The objective of this chapter is to examine the role of shear stress transfer in injection-induced fracture activation. Fluid injection tests on critically stressed fractures are conducted to observe different slip modes. The two slip modes with uniformly and non-uniformly distributed fluid pressures are reproduced using hydromechanical modeling. The fluid pressure distribution, slip displacement and shear stress change in the uniformly and non-uniformly pressurized fractures are compared. The competing propagation among fluid pressure front, slip front and shear stress transfer front is highlighted. The obtained results are compared with the field data to implicate the mechanism of injection-induced seismicity.

6.2 Methods

6.2.1 Experimental setup and procedure

A series of fluid injection tests were carried out on a sawcut fracture in Bukit Timah granite to simulate the injection-induced activation of a critically stressed fracture using the triaxial shear-flow setup (Figure 3-11). The mineralogical, physical, mechanical and hydraulic properties of the granite are listed in Table A-1. An inclined fracture was cut at 30° with respect to the longitudinal axis of a core sample with 50 mm diameter and 100 mm height, and polished the fracture surfaces using fine sandpaper with 25.6 μm particle size. Two boreholes with 2 mm diameter were drilled from the sample ends to the fracture as the injection and monitoring boreholes. Two layers of Teflon jacket were used to fix the sample on the coreholders and to isolate the sample from the confining fluid. Two pieces of filter paper with 0.45 μm pore size were inserted between the sample ends and coreholders to prevent sheared-off particles from flowing into the pore lines.

In the fluid injection tests, silicon oil and distilled water were used as the confining and pore fluids, respectively. The shear and normal stresses on the fracture were directly managed by servo-controlling the deviator stress and confining pressure. The undesired stress induced by the deformation of Teflon jacket and the deviation

of fracture surfaces was subtracted from the deviator stress. The load point displacement was measured by LVDT. The axial shortening was obtained by deducting the deformation of the test machine and granite material from the load point displacement. The slip displacement of the fracture was estimated as the component of the axial shortening along the fracture. The shear stress, slip displacement, injection and monitoring pressures were recorded at a sampling rate of 100 Hz.

The test procedure is depicted in Figure 6-1a, using Case 1 in Table 6-1 as an example. The shear stress was increased by advancing the load point displacement at $1\text{ }\mu\text{m/s}$ under 6 MPa normal stress until the fracture slip occurred to obtain the shear strength. A critically stressed fracture was then simulated by applying a shear stress as 80% of the shear strength under the same normal stress. Lastly, the water was injected at a volumetric rate of 0.05 mL/min through the injection borehole to reactivate the fracture. The shear stress, slip displacement, injection and monitoring pressures were recorded at a sampling rate of 100 Hz. The permeability of the fracture before and after the test was measured using the steady-state flow technique (Scheidegger, 1958). Because the width of the elliptical fracture is changing along the flow direction, the fluid flow region is simplified as a rectangle with the same area as the fracture (Figure 6-2). A constant width is thus calculated by setting the length as the distance between the injection and monitoring boreholes (Ye and Ghassemi, 2018). Five tests were performed under various combinations of normal stress (6, 11 and 21 MPa) and injection rate (0.05, 0.25 and 1.25 mL/min).

(a)

(b)

(c)

Figure 6-1 (a) Test procedure, including critically stressed fracture simulation and fluid injection process. (b) Numerical modeling and zoomed-in view highlighting particle, pore space, smooth joint, and flow channel. (c) Numerical validation in comparison to test data based on degree of fluid pressure heterogeneity.

Figure 6-2 Fluid flow region for fracture permeability estimation (modified from Ye and Ghassemi, 2018).

6.2.2 Numerical simulation and validation

A hydromechanical coupled fluid flow model based on the two-dimensional PFC2D was used to uncover the evolution of fluid pressure and shear stress on the fracture during the tests (Itasca, 2002). The hydro-mechanical coupled discrete particles joints model has been used successfully to mimic the two-way coupled hydro-mechanical process in the fractured reservoirs (Yoon et al., 2014; Yoon et al., 2015; Zang et al., 2013). Figure 6-1b presents that the core sample is represented as an assembly of circular particles bonded at their contacts using the parallel bond contact model, and the inclined fracture is simulated using the smooth joint contact model. Using this contact model allows modelling the fracture as a combination of small slipping patches that are oriented in the direction of the fracture but placed offset. Unlike in Cappa et al. (2018) where the fracture is modeled as a straight planar structure, this model provides a unique way of representing a natural fracture in a more heterogeneous way, and allows stress state heterogeneity. Also, the failure of smooth joint contacts is modelled as a dynamic process where the accumulated strain energy is released as seismic energy and affects the failure of the neighboring contacts by dynamic and static stress triggering. The mechanisms involving stress accumulation, strain energy release by contact failure, propagation and attenuation, and static/dynamic stress triggering are all taken into consideration in the model. Each particle contact is assumed as a flow channel, which connects adjacent pore spaces with pressurized fluid. The fluid flow in the channel is assumed laminar, and the flow rate is associated with the hydraulic aperture of the channel governed by the cubic law (Zimmerman and Bodvarsson, 1996). The hydraulic aperture is determined by the effective normal stress on particle contacts as well as aperture sizes under infinite and zero normal stresses (Hökmark et al., 2010). The increase in fluid pressure is estimated based on the bulk modulus of fluid, volume of pore space and sum of fluid volume in time steps, resulting in particle rearrangement (Al-Busaidi, 2005). In turn, the particle displacement alters the inter-particle force, hydraulic aperture, and fluid pressure.

Particularly, in the numerical study, the material simulated, in this case a reservoir rock mass, is modelled as an aggregate of circular particles bonded at their

contacting points with finite thickness of cementing around the contact with the Mohr-Coulomb strength parameters. Under an applied load, the bonds can break in Mode I (tensile) or Mode II (shear). The calculation cycle in PFC2D is a time stepping algorithm that requires repeated application of the law of motion applied to each particle and a linear force displacement law applied to each contact. Upon the bonded particle assembly, a fracture plane is inserted and modeled using smooth joint contact model (Figure 6-1b). More details can be found in Potyondy and Cundall (2004) and Mas Ivars et al. (2011).

Flow of viscous fluid in a bonded particle assembly and fluid pressure and volume driven breakages of bonds in Mode I and Mode II are simulated. Fluid flow is simulated by assuming that each particle bonded contacts is a flow channel and these channels connect up pore spaces that can store pressure. Pressure-driven flow of viscous fluid between the two pores spaces is governed by the cubic law assuming that the flow is laminar between two smooth parallel plates. The fluid flow model is also programmed so that the hydraulic aperture at particle contacts changes as a function of normal stress at the particle contacts. The experimentally derived hydraulic aperture vs normal stress (σ_n) relation from Hökmark et al. (2010) is used,

$$e_f = e_{inf} + (e_0 - e_{inf}) \exp(\alpha \sigma'_n) \quad (6-1)$$

where e_{inf} is the hydraulic aperture at infinite normal stress, e_0 is the hydraulic aperture at zero normal stress, and α defines how sensitive the hydraulic aperture is to the normal stress increase. These parameters are calibrated based on the experimentally derived permeability of the fracture.

Fluid pressure increase per time step in a pore space is computed from the fluid bulk modulus (K_f), volume of pore space (V_d), sum of net flow volume (q), and pore volume change due to mechanical effect (ΔV_d),

$$\Delta p_f = \frac{K_f}{V_d} (q \Delta t - \Delta V_d) \quad (6-2)$$

The fluid exerts pressure on the surrounding particles causing deformations. The resulting force then changes the stress states at the surrounding contacts which in

turns changes the hydraulic aperture and thereby flow field. In this way, the fluid flow model handles two-way coupling. The micro-scale parameters used in the smooth joint model are listed in Table 6-2.

Figure 6-1a shows that the injection pressure steadily rises, followed by a delayed increase in monitoring pressure. To quantify the variation of the pressure difference between the injection and monitoring boreholes, the degree of fluid pressure heterogeneity is defined as the ratio of the difference between the injection and monitoring pressures to the injection pressure. The numerical model is validated by comparing the degrees of fluid pressure heterogeneity obtained from the experimental and numerical studies of Case 1 (Figure 6-1c). The elapsed time is normalized by the injection duration. Both the results exhibit that the degree of fluid pressure heterogeneity slightly decreases at the beginning of fluid injection and dramatically drops in the uniformly pressurized fracture, indicating that the numerical model is reliable to simulate the fracture behaviors in the tests. Because the non-uniformly distributed fluid and the associated heterogeneous slip are highly complex in the other cases and unable to calibrate the numerical model, the validated case is used to examine the influence of injection rate on fluid pressure distribution and shear stress transfer.

Table 6-1 Test conditions and results.

Case No.	Normal stress and pore pressure σ_n P (MPa)	Shear strength (MPa)	Critical shear stress (MPa)	Injection rate (mL/min)	Injection pressure at failure (MPa)	Monitoring pressure at failure (MPa)	Degree of fluid pressure heterogeneity	Shear stress drop (MPa)	Slip displacement (mm)	Peak slip velocity (mm/s)	Initial fracture permeability (m ²)	Dynamic friction coefficient
1	$\sigma_n=6$, $P=0.05$	3.65	2.92	0.05	1.67	1.65	0.012	0.60	0.06	1.05	2.25×10^{-12}	0.53
2	$\sigma_n=11$, $P=0.2$	6.94	5.55	0.05	2.95	2.17	0.264	1.37	0.13	2.64	9.39×10^{-13}	0.50
3	$\sigma_n=11$, $P=0.2$	7.56	6.05	0.25	2.78	1.15	0.586	1.04	0.14	1.48	2.54×10^{-13}	0.55
4	$\sigma_n=11$, $P=0.4$	7.99	6.39	1.25	2.86	0.13	0.955	1.10	0.15	1.63	9.40×10^{-13}	0.56
5	$\sigma_n=21$, $P=2$	14.34	11.47	0.25	7.62	0.013	0.998	2.08	0.72	3.95	5.07×10^{-14}	0.55

Table 6-2 Micro-parameters in the smooth joint model.

Smooth joint model property	Value (unit)
Normal stiffness	303 (GPa/m)
Shear stiffness	50 (GPa/m)
Tensile strength	0.1 (MPa)
Cohesion	0.5 (MPa)
Friction angle	0 (deg)
Dilation angle	0 (deg)
Coefficient for frictional sliding	0.8

6.3 Results

The results demonstrate that the fracture slip occurs when the monitoring pressure approaches the injection pressure, and is accompanied by a sudden shear stress drop and an abrupt slip displacement jump (Figure 6-1a). However, the degree of fluid pressure heterogeneity increases with higher normal stress and faster injection rate, and the resulting slip behaviors are different in the other cases (Table 6-1). Figure 6-3a reproduces the time-dependent evolution of fluid pressure along the major axis of the fracture at an injection rate of 0.05 mL/min. The fluid pressure initially accumulates around the injection borehole, and subsequently diffuses towards the monitoring borehole. The fluid pressure around the monitoring borehole finally becomes higher than the fluid pressure at fracture failure (0.75 MPa), which is predicted by the Mohr-Coulomb failure criterion established based on the shear strengths in Table 6-1. The fracture is thus reactivated by the overall reduction in effective normal stress on the fracture. After that, the injection rate is solely increased to 1.25 mL/min in the validated case (Figure 6-3b). The highly pressurized fluid is constrained near the injection borehole, and rapidly exceeds the fluid pressure at fracture failure. The fracture instability is attributed to injection-induced dynamic slip, which initiates near the injection borehole and propagates across the fracture (Garagash and Germanovich, 2012; Rutter and Hackston, 2017).

The slip displacement along the major axis of the fracture in both the cases is heterogeneous and dependent on the non-uniform distribution of fluid pressure. The injected fluid may be unevenly accumulated at the beginning of fluid injection, which could explain why the slip displacement close to the injection borehole slightly deviates around the initial value. After the fracture fully deforms along the shear direction in the case with low injection rate, the dynamic slip occurs at 170s (Figure 6-3c). However, the high injection rate accelerates the aseismic creep until the fracture fails at 10s (Figure 6-3d). The shear stress change with respect to the initial shear stress along the major axis further explains the slip behaviors at different injection rates. The effective normal stress on the fracture patch near the injection borehole can decrease locally with increasing fluid pressure, leading to the reduction in local frictional stress on this patch. The local frictional stress on the

patch near the monitoring borehole thus increases to balance the applied shear stress. For the low injection rate, the shear stress gradient over the fracture is low due to the minor fluid pressure gradient along the major axis of the fracture (Figure 6-3a). Hence, the occurrence of fracture slip is accompanied by roughly uniform drop of shear stress (Figure 6-3e). The high injection rate causes the dramatic build-up of fluid pressure near the injection borehole (Figure 6-3b) and the high shear stress gradient in a short duration (Figure 6-3f). As a result, the shear stress over the fracture slightly decreases during the aseismic slip, followed by the shear stress near the monitoring borehole sharply dropping during the seismic slip.

The numerical results demonstrate that the fracture slip is a consequence of injection-induced shear stress imbalance along the fracture. The fluid pressure front, slip front and shear stress transfer front may play different roles in fracture activation. The fluid pressure front is defined as the instantaneous boundary of pressurized fluid, on which the fluid pressure is equal to zero. The slip front is the minimal value of slip displacement, beyond which the slip displacement steadily increases along the shear direction. The shear stress transfer front is the turning point of shear stress change, at which the shear stress change transitions from negative to positive. At the low injection rate, the fluid pressure front propagates at a constant speed along the fracture, and always outpaces the slip and shear stress transfer fronts (Figure 6-3g). The resulting change in effective normal stress influences the slip and shear stress transfer fronts, which move simultaneously and speed up along the uniformly overpressurized fracture. The slip and shear stress transfer fronts initially lag behind the fluid pressure front in the case with high injection rate (Figure 6-3h), but successively exceed the fluid pressure front. The acceleration of slip front is due to the aseismic creep, and the shear stress transfer front is further driven by the high gradient of shear stress. However, the shear stress transfer front runs slightly after the slip front in the short duration, presumably owing to the slow deformation of the granite material adjacent to the fracture (Wang et al., 2020).

(a)

(b)

(c)

(d)

(e)

(f)

(g)

(h)

Figure 6-3 Numerical results of injection-induced fracture activation under the same stress state (6 MPa normal stress and 80% shear strength) at different injection rates (0.05 and 1.25 mL/min in the cases shown on the left and right columns, respectively). The red and blue solid circles denote the locations of injection and monitoring boreholes along the major axis of the fracture, respectively. The blue dashed lines in (a) and (b) indicate the fluid pressure at fracture failure predicted by the Mohr-Coulomb failure criterion. The time in (g) and (h) is normalized by

injection duration, and the colors and shapes of these points are corresponding to those in (a)-(f) with the same time sequence.

6.4 Discussion

6.4.1 Shear stress transfer due to fluid injection

The results illustrate that the shear stress transfer front changes from slow to fast speed (i.e., the slope of shear stress transfer front) with a short transition period at the low injection rate. The occurrence of transition period is due to the nearly uniform distribution of fluid pressure and fully deformed fracture along the shear direction. However, the transition period is early-occurring and long-lasting at the high injection rate. The aseismic slip dominates this period, and modifies the competing propagation among the fluid pressure front, slip front and shear stress transfer front.

(a)

(b)

(c)

Figure 6-4 Determination of fluid pressure front, slip front and shear stress transfer front from numerical simulations on the field fluid injection test (Cappa et al., 2018). The data is collected from the case at an initial stress ratio of 0.47 with rate-state friction at a time interval of 150 s. (a) Collection of the ratio between slip front and fluid pressure front (modified from Fig. 3D). (b) Determination of slip front and

fluid pressure front based on the ratio (modified from Fig. 3C). The fluid pressure front is determined as the boundary of zero fluid pressure. The starting points of continuous shear displacement increase are chosen as the slip fronts in both cases. (c) Determination of shear stress transfer front (modified from Fig. S2B). The shear stress transfer front is assessed as the turning point where the shear stress starts to decrease and the same as the turning point of shear stress change from negative to positive in the cases in this study.

The transition from aseismic to seismic slip is observed in a field fluid injection test on a naturally, initially inactive fracture in a carbonate formation (Guglielmi, Cappa, et al., 2015). Particularly, the aseismic slip during the test is simulated using a hydromechanical model to uncover the field-scale evolution of fluid pressure front, slip front and shear stress transfer front. The data points of the three fronts are extracted based on the same definitions in Figure 6-3 from the field-based study (Figure 6-4) to verify the competing propagation among the fronts during the aseismic slip observed in the laboratory-based study. Although the absolute values from the two models are not comparable because of dissimilar stress and injection conditions, the evolutionary trends in the fluid pressure front, slip front and shear stress transfer front are similar during the aseismic slip (Figure 6-3h and Figure 6-5a). The slip and shear stress transfer fronts successively outpace the fluid pressure front, and steeply rise at similar increment rates. The seismic slip occurs in the following time period but is not considered in Cappa et al. (2018), and the field data may not be suitable to compare directly with the experimental data due to different sensor types and sampling frequencies.

Shear stress transfer is known as a critical mechanism for the occurrence of natural earthquakes, where small, sudden stress perturbation could cause the seismic slip (Stein, 1999). In analogue to injection-induced earthquakes, fluid injection serves as the stress perturbation. The resulting reduction in effective normal stress amplifies the gradient of shear stress, which is low in the vicinity of injection borehole and high in the far field. Large shear stress gradient associated with high degree of fluid

pressure heterogeneity thus promotes dynamic slip from the pressurized to unpressurized fracture segments.

6.4.2 Implications for injection-induced fracture activation

The results reveal that the shear stress transfer induced by fluid overpressurization controls the size of the seismic events. Thus, the maximum seismic moment of in the cases in this study is calculated using $M_0(\max) = GAd$ (Aki, 1966), where G is the combined rigidity of the test system and the fracture (1.2 GPa) and determined by Ji et al. (2019), A is the fracture area ($3.9 \times 10^{-3} \text{ m}^2$), and d is the slip displacement (Table 6-1). Here the maximum slip displacement of a seismic event is used to assess the maximum moment release. However, for non-uniformly pressurized fractures, the maximum moment release could be underestimated as the laboratory-scale fracture cannot fully accommodate a fault rupture.

(a)

(b)

(c)

Figure 6-5 (a) Estimation of fluid pressure front, slip front and shear stress transfer front in a field fluid injection test (Cappa et al., 2018). (b) Maximum seismic moment as a function of injected volume (modified from Buijze et al., 2015; Galis et al., 2017). Field data collected from injection-induced earthquakes in crystalline and clastic rocks in comparison to the models $M_0(\text{max}) = G\Delta V$ (black solid line), where $G = 30$ GPa, as well as $M_0(\text{max}) = \Sigma \Delta V^{3/2}$ and $M_0(\text{max-arr}) = \gamma \Delta V^{3/2}$ (blue dashed lines), where $\gamma = 1.5 \times 10^6$, 1.5×10^8 and 1.5×10^{10} , and equivalent $\Sigma = -1.95$, -0.6 and 0.7 . (c) Test data in this study in comparison to the models, where $G = 1.2$ GPa (red solid line), $\gamma = 3.1 \times 10^{11}$ and equivalent $\Sigma = 1.6$ (red dashed line).

Three analytical models are commonly used to predict the upper bound of seismic moment ($M_0(\max)$) based on injected volume (ΔV), which oversimplifies the geological and injection conditions but is workable here to distinguish the seismic events associated with different degrees of fluid pressure heterogeneity. The first model proposed by McGarr (2014) is $M_0(\max)=G\Delta V$, which assumes that fracture slip is confined within the stimulated reservoirs. The second model from van der Elst et al. (2016) is $M_0(\max)=\Sigma\Delta V^{3/2}$, where Σ is the seismogenic index, and established based on the sampling statistics of the Gutenberg-Richter distribution of natural earthquakes. The last model from Galis et al. (2017) is $M_0(\max\text{-arr})=\gamma\Delta V^{3/2}$, where γ is the reservoir parameter, and suitable to predict the maximum seismic moment of self-arrested events. The three models are plotted in Figure 6-5b, in which G is considered as a typical modulus of rigidity for crustal rocks (30 GPa), γ is three possible values (1.5×10^6 , 1.5×10^8 and 1.5×10^{10}), and Σ is determined when the models from van der Elst et al. (2016) and Galis et al. (2017) are equivalent. The injection-induced earthquakes from various field cases (Buijze et al., 2015) are also divided into two categories based on the types of reservoir rocks (i.e., crystalline and clastic rocks). Figure 6-5b presents that the maximum seismic moment of injection-induced earthquakes increases roughly with larger injected volume in both crystalline and clastic rocks, which is consistent with the three analytical models. Most of the data points standing for the seismic events in crystalline rocks and all the data points for those in clastic rocks are below the upper bounds, indicating that most of the seismic events are self-arrested. The outliers, representing the 1974 M_w 2.8 Matsushiro earthquakes (Ohtake, 1974) and the 2017 M_w 5.5 Pohang earthquakes (Lee et al., 2019), are classified as runaway earthquakes. In these cases, pore pressure perturbation causes the rupture arrest area approaching fracture area (Galis et al., 2017), triggering seismic energy release from tectonically controlled patches beyond the reservoirs and leading to destructive earthquakes (Woo et al., 2019).

The cases in this study are not comparable to field cases, in terms of geological and injection conditions. However, the three models are applicable to the cases in this study. In the cases in this study, the models are constructed based on $G=1.2\text{ GPa}$,

$\gamma=3.1\times10^{11}$ and the equivalent $\Sigma=1.6$ (Ji et al., 2019). The upper bounds cap the maximum seismic moment of Cases 1-3 with low degrees of fluid pressure heterogeneity, whereas those are invalid for the maximum seismic moment of Cases 4 and 5 with high degrees of fluid pressure heterogeneity, because these cases situate above the upper bounds. If the fracture area could accommodate the rupture area, the estimated moment release may be even larger than the upper bounds. The slip displacement jumps accompanied by shear stress drop, which is related to the development of shear stress gradient and occurs uniformly or non-uniformly along the fracture depending on the degree of fluid pressure heterogeneity (Figure 6-3e-f). Therefore, the shear stress transfer strongly affects the seismic moment release. This observation may also explain that runaway earthquakes due to fluid injection have only been observed in crystalline rocks (Figure 6-5b). The stiffness of crystalline rocks is typically higher than that of clastic rocks. The crystalline rocks are thus more readily to bear high fluid overpressure and to develop large shear stress gradient, resulting in devastating earthquakes.

6.5 Summary

Experimental and numerical studies were conducted on injection-induced activation of uniformly and non-uniformly pressurized fractures to shed light on the role of shear stress transfer in fracture activation. The experimental results show that the degree of fluid pressure heterogeneity controlled by the stress and injection conditions strongly influences the fracture behaviors. The numerical results demonstrate that the uniformly pressurized fracture is activated accompanied by global drop of shear stress and full deformation along the shear direction. For the non-uniformly pressurized fracture, highly heterogeneous fluid pressure accumulates near the injection borehole, resulting in uniform and localized drops of shear stress during the aseismic and seismic events, respectively. The fluid pressure front always outpaces the slip and shear stress transfer fronts on the uniformly pressurized fracture, but lags behind on the non-uniformly pressurized fracture after the aseismic slip, which is consistent with the results of a field fluid injection test on a natural fracture in a carbonate formation. The data also indicate that the prediction of the maximum seismic moment should rely not only on injected volume but also

on the degree of fluid pressure heterogeneity, especially for non-uniformly pressurized fractures.

CHAPTER 7 MITIGATING OF INJECTION-INDUCED SEISMICITY ON UNDRAINED FRACTURES USING CYCLIC FLUID INJECTION

7.1 Introduction

The injection-induced fluid pressurization could either weaken fully drained fractures through uniform reduction in effective normal stress, or destabilize locally undrained fractures through dynamic rupture extending beyond the reservoirs. The shear slip of natural and hydraulically induced fractures within the reservoirs is expected to improve the efficiency of energy production (Wu et al., 2017; Ye and Ghassemi, 2018). However, the unintended perturbation of adjacent tectonic faults is believed as the main cause of destructive earthquakes (Eyre et al., 2019; Galis et al., 2017; Woo et al., 2019), which has become a major barrier to the development of unconventional energy.

Several attempts have been made to mitigate the seismic risks associated with fluid injection, such as traffic light systems (Bommer et al., 2006) and borehole seismometer networks (Kwiatek et al., 2019). Cyclic fluid injection has also been proved as an effective and controllable strategy to manage the anthropogenic earthquakes. Both the number and magnitude of the seismic events decrease with cyclic fluid injection in Äspö Hard Rock Laboratory, Sweden (Kwiatek et al., 2018; Zang et al., 2017) and Pohang enhanced geothermal system (EGS) project, Korea (Hofmann et al., 2019; Hofmann et al., 2018). Nevertheless, the current field tests with pre-determined injection parameters may not accommodate the dynamic evolution of the reservoirs during fluid injection. To further improve the effectiveness of cyclic fluid injection, it is critical to examine the cycle-related factors controlling fracture activation.

Laboratory fluid injection tests have advantages to simulate the in-situ stress and fluid injection conditions and to isolate the cycle-related factors from complex geologic environments. Previous test results indicate that the dynamic slip of uniformly pressurized fractures occurs when the fluid pressure at fracture failure reaches that predicted by the Mohr-Coulomb failure criterion, but the fluid pressure at fracture failure can overshoot the predicted pressure on non-uniformly

pressurized fractures (Rutter and Hackston, 2017; Scuderi et al., 2017). The fluid overpressure on locally undrained fractures may cause the slip area exceeding the pressurized area, resulting in the fracture slip propagating in the unpressurized area (Azad et al., 2017; Cappa et al., 2018). Cyclic fluid injection into simulated fractures in permeable sandstone samples promotes the transition from the stable to unstable slip (Noël et al., 2019), while cyclic fluid injection into impermeably granite samples exhibits lower magnitudes of the largest seismic event compared to monotonic fluid injection (Zang et al., 2019; Zhuang et al., 2019). These results suggest that the drainage conditions of the fractures and adjacent rocks strongly influence the occurrence of the seismic events. Particularly, the injection-induced slip of locally undrained fractures most likely leads to felt earthquakes, but its response to cyclic fluid injection remains poorly understood. Additionally, the pressure-controlled fluid injection is commonly adopted as it is readily controlled in the laboratory (e.g., Rutter and Hackston, 2017; Scuderi et al., 2017; Ye and Ghassemi, 2018; Wang et al., 2020). However, the volume-controlled fluid injection is realistically operated in the field (e.g., Hofmann et al., 2019; Kwiatek et al., 2018; 2019), which should be carefully managed in the laboratory to avoid a dramatic increase in fluid pressure and to unexpectedly cause sample jacket breakage and confining oil leakage.

In this chapter, fluid injection tests on locally undrained fractures are conducted to provide insights into cyclic fluid injection as a solution for seismic risk mitigation. Both the pressure-controlled and volume-controlled fluid injection conditions are considered to understand the effect of different cyclic injection conditions on seismic risk mitigation. The seismic moment, seismic energy, and hydraulic energy of the monotonic and cyclic injection tests as well as previous field injection tests are also compared to highlight the effectiveness of cyclic fluid injection. The possible development of cyclic fluid injection in seismic risk mitigation are finally discussed.

7.2 Material and methods

A series of fluid injection tests was carried out on sawcut fractures in Bukit Timah granite. The mineralogical, physical, mechanical, and hydraulic properties of this granite are listed in Table A-1.

Figure 7-1 Illustration of fluid injection strategies during (a) pressure-controlled monotonic injection, (b) pressure-controlled cyclic injection, (c) volume-controlled monotonic injection, and (d) volume-controlled cyclic injection. The red line indicates the duration of injection suspension (300 s).

A granite sample was selected from a 50-mm-diameter cylindrical core without visible cracks on the surfaces and was ground with flat and parallel ends to 100 mm long. A diamond saw was used to cut the sample at 30° to the core axis, and the sawcut surfaces were polished using sandpaper with $25.6 \mu\text{m}$ particle size. The

sample was fixed on the coreholders and isolated from the confining oil using two layers of Teflon jackets. Distilled water was injected into the fracture through two 2-mm-diameter boreholes, and the fluid pressures were monitored by dual hydraulic pumps.

MTS 815 rock mechanics test system was used to conduct the fluid injection tests (Figure 3-11). The fluid was injected through the lower borehole, and both the fluid pressures in the lower (injection) and upper (monitoring) boreholes were measured by the VP-12K Vindum dual syringe pumps (Figure 3-3). The shear and normal stresses on the fracture were servo-controlled by the deviator stress and confining pressure and determined using Eqs. (3-2) and (3-3), respectively. The load point displacement was recorded by LVDT. The shear displacement along the fracture was obtained by Eq. (3-4). The stress and displacement were measured at a sampling frequency of 100 Hz, while the fluid pressures were recorded at a sampling rate of 1 Hz.

The monotonic and cyclic fluid injection tests were performed under pressure-controlled and volume-controlled conditions, respectively (Table 7-1). A normal stress (i.e., 11 and 21 MPa) was first applied, and the sawcut fracture was saturated with 1 MPa fluid pressure. It is assumed that this system and fluid are incompressible during the tests. Second, the load point displacement was increased at a constant rate of 1 $\mu\text{m/s}$ until the fracture failed to obtain the shear strength. Third, the shear stress was maintained as 80% of the shear strength, and it is assumed that the fracture was approaching a critical stress state. The fluid was subsequently injected through the injection borehole sequentially using one of the four injection strategies until the fracture activation occurs again in different fluid injection tests. For the pressure-controlled monotonic injection tests, the fluid was injected at a pressure rate of 0.01 MPa/s (Figure 7-1a). The pressure-controlled cyclic injection tests were conducted at the same pressure rate by changing the fluid pressure between the initial injection pressure (1 MPa) and critical injection pressure (Figure 7-1b), which is defined as 95% of the injection pressure at fracture failure obtained in the pressure-controlled monotonic injection test. In the volume-

controlled monotonic injection tests, the fluid was injected at a volume rate of 0.2 mL/min (Figure 7-1c). The injection rate in the volume-controlled cyclic injection tests alternated between 0.2 and 0 mL/min, and each cycle was terminated when the critical injection pressure (e.g., 95%, 90% and 85% of the injection pressure at fracture failure obtained in the volume-controlled monotonic injection test) was achieved (Figure 7-1d). The duration of injection suspension (0 mL/min) between the adjacent cycles is 300 s.

7.3 Results

The fluid injection test results show that the failure of the fracture is accompanied by a sudden shear stress drop and an abrupt shear displacement jump (Figure 7-2 and Figure 7-3), which is known as dynamic slip (Tinti et al., 2016). The occurrence of dynamic slip is because that the slip weakening rate of the fracture is higher than the elastic unloading rate of the test system (Rice, 1983). After that, when the shear displacement further increases, the shear stress recovers to the initial value due to the re-formation and re-strengthening of asperity contacts (Marone, 1998a; Shreedharan et al., 2020).

For the pressure-controlled monotonic injection test under 11 MPa normal stress, the fracture slip occurs when the injection pressure rises to 3.68 MPa, while the monitoring pressure at fracture failure is only 1.0 MPa (Figure 7-2a). The critical injection pressure in the pressure-controlled cyclic injection test is set as 3.50 MPa (i.e., 95% of 3.68 MPa). During the first cycle, the injection pressure increases to the critical injection pressure, and subsequently decreases to the initial injection pressure. The fracture slip occurs when the injection pressure reduces to 3.36 MPa (Figure 7-2b). The monitoring pressure at fracture failure remains very low (1.1 MPa). The difference between the injection and monitoring pressures at fracture failure reveals that the failure of the fracture can be attributed to the injection-induced fracture slip, which initiates near the injection borehole and propagates to destabilize the entire fracture (Jia et al., 2020). To better observe the fracture response to this fluid injection strategy, another pressure-controlled cyclic injection test was performed with a critical injection pressure of 3.33 MPa (i.e., 90% of 3.68

MPa). The results show that no slip occurs when the monitoring pressure reaches 3 MPa in the 33rd cycle (Figure 7-2c), which is the fluid pressure at fracture failure predicted by the Mohr-Coulomb failure criterion. This is mostly due to the enhanced shear strength caused by frictional healing after a long-term fluid injection. The shear displacement slightly increases with fluctuations, indicating stable fracture creep occurring with the cyclic injection and associated with poro-elastic coupling of stress and fluid pressure (Wang et al., 2020). When the normal stress is 21 MPa and the critical injection pressure is 13.94 MPa (i.e., 95% of 14.67 MPa), the fracture also fails during the first cycle in the pressure-controlled cyclic injection test, and the difference between the injection and monitoring pressures becomes larger than that under 11 MPa normal stress (Table 7-1). The results indicate that cyclic fluid injection could be inefficient if the critical injection pressure is very close to the predicted pressure at fracture failure.

(a)

(b)

(c)

Figure 7-2 Evolution of shear stress, shear displacement as well as injection and monitoring pressures during (a) pressure-controlled monotonic injection, (b) pressure-controlled cyclic injection with fracture slip and (c) pressure-controlled cyclic injection with no slip under 11 MPa normal stress.

Similarly, as shown in Figure 7-3a, the volume-controlled monotonic injection test under 11 MPa normal stress is used to estimate the injection pressure at fracture failure (3.72 MPa), and the corresponding volume-controlled cyclic injection tests are conducted with three critical injection pressures (3.53, 3.38 and 3.16 MPa, as

95%, 90% and 85% of 3.72 MPa, respectively). When the critical injection pressures are 3.53 and 3.38 MPa, the fracture instability occurs during the first cycle, and the monitoring pressures at fracture failure are still around 1.0 MPa. However, when the critical injection pressure further decreases to 3.16 MPa, the fracture is reactivated during the 27th injection cycle (Figure 7-3b). Importantly, the monitoring pressure at fracture failure almost reaches the injection pressure, indicating that cyclic fluid injection significantly promotes the uniform distribution of fluid pressure on the fracture. Moreover, this test also shows a profound stable fracture creep before the occurrence of dynamic slip. For all the cyclic fluid injection tests under 21 MPa normal stress, the fracture fails during the first cycle, because the fluid is largely constrained in the highly squeezed fracture. The further decrease in the critical fluid pressure may enhance the effectiveness of cyclic fluid injection.

(a)

(b)

Figure 7-3 Evolution of shear stress, shear displacement as well as injection and monitoring pressures during (a) volume-controlled monotonic injection and (b) volume-controlled cyclic injection under 11 MPa normal stress.

The results show that the injection and monitoring pressures vary at different growth rates during cyclic fluid injection, depending on normal stress and critical injection pressure. The transition from locally undrained to fully drained condition is accompanied by reducing asperity mechanical interaction on the fracture and advancing pressure spike formation between sheared asperities. The reduced asperity mechanical interaction associated with lesser and weaker asperity contacts is presumably due to the more uniform reduction in effective normal stress during the transition from locally undrained to fully drained condition (Dieterich and Kilgore, 1994; Kilgore et al., 2017; Shreedharan et al., 2019). The degree of fluid pressure heterogeneity (D_f) is used to quantify fluid pressure distribution on the fracture, which is defined as the ratio of the difference between injection (P_{inj}) and monitoring (P_{mon}) pressures to the injection pressure:

$$D_f = \frac{P_{inj} - P_{mon}}{P_{inj}} \quad (7-1)$$

Table 7-1 Summary of test conditions and results. Here a and b indicate the fluid injection tests under 11 and 21 MPa normal stresses, respectively; M and C mean the monotonic and cyclic injection tests, respectively; P and V are the pressure-controlled and volume-controlled injection tests, respectively; $P_{f, inj, aMP}$, $P_{f, inj, aMV}$, $P_{f, inj, bMP}$ and $P_{f, inj, bMV}$ denote the injection pressures at fracture failure in tests aMP, aMV, bMP and bMV, respectively.

Test	Injection rate	Cycle numbers until failure	Critical injection pressure ratio	Critical injection pressure (MPa)	Monitoring pressure at failure (kPa)	Injected volume (mm^3)	Shear stress drop (MPa)	Slip displacement (μm)	Dynamic friction coefficient
aMP	0.01 MPa/s	-	$P_{f, inj, aMP}$	3.68	1027.6	817.33	0.53	44.01	0.419
aCP1	0.01 & -0.01 MPa/s	1	95% $P_{f, inj, aMP}$	3.36	1050.1	684.29	0.57	28.00	0.412
aCP2	0.01 & -0.01 MPa/s	33	90% $P_{f, inj, aMP}$	3.33	3000.0	674.57	-	-	-
aMV	0.2 mL/min	-	$P_{f, inj, aMV}$	3.72	1018.4	763.02	0.47	39.40	0.432
aCV1	0.2 & 0 mL/min	1	95% $P_{f, inj, aMV}$	3.43	1003.9	692.43	0.42	30.85	0.427
aCV2	0.2 & 0 mL/min	1	90% $P_{f, inj, aMV}$	3.23	1008.7	653.57	0.37	30.28	0.429
aCV3	0.2 & 0 mL/min	27	85% $P_{f, inj, aMV}$	3.19	3073.0	684.29	0.58	38.35	0.457
bMP	0.01 MPa/s	-	$P_{f, inj, bMP}$	14.67	1002.5	2766.80	0.71	79.08	0.556
bCP1	0.01 & -0.01 MPa/s	1	95% ($P_{f, inj, bMP}$)	12.65	1019.7	2295.26	0.66	33.33	0.513
bMV	0.2 mL/min	-	$P_{f, inj, bMV}$	14.54	1011.2	2570.42	0.98	50.96	0.648
bCV1	0.2 & 0 mL/min	1	95% $P_{f, inj, bMV}$	13.77	1011.5	2467.56	1.24	50.18	0.521
bCV2	0.2 & 0 mL/min	1	90% $P_{f, inj, bMV}$	12.99	1017.2	2336.55	0.87	45.08	0.535
bCV3	0.2 & 0 mL/min	1	85% $P_{f, inj, bMV}$	12.25	1021.1	2188.17	1.04	41.53	0.511

Figure 7-4 Degree of fluid pressure heterogeneity reduces with decreasing ratio between the critical injection pressure and the injection pressure at failure in both cases of pressure-control and volume-control fluid injection under 11 MPa (black lines) and 21 MPa (blue lines) normal stresses, respectively. Values 0 and 1 denote the uniform fluid pressure distribution on the fracture and non-uniform fluid pressure distribution with fluid pressure only accumulating around the injection borehole.

Lower degree of fluid pressure heterogeneity signifies less heterogeneous fluid pressure distribution on the fracture. Figure 7-4 presents the degree of fluid pressure heterogeneity as a function of the ratio between the critical fluid pressure and the injection pressure at fracture failure under 11 MPa and 21 MPa normal stresses. The ratio is considered as 100% in the pressure-controlled and volume-controlled monotonic injection tests. For the pressure-controlled cyclic injection tests under both the normal stresses, the degree of fluid pressure heterogeneity slightly decreases. The volume-controlled cyclic injection tests remarkably reduce the degree of fluid pressure heterogeneity under 11 MPa normal stress, and the reduction is promoted when the critical injection pressure further drops from the injection pressure at fracture failure. However, the degree of fluid pressure heterogeneity remains close to 1 under 21 MPa normal stress, suggesting that cyclic fluid injection is less effective in promoting fluid pressure migration on the fracture

under higher normal stress. A lower critical injection pressure and lower injection frequency are expected under higher normal stress to achieve a uniform fluid pressure distribution on the fracture.

7.4 Discussion

7.4.1 Seismic moment release due to cyclic fluid injection

The injection-induced seismic moment can be predicted using injected volume (Galis et al., 2017; McGarr, 2014; McGarr and Barbour, 2018). Although the prediction accuracy is affected by several assumptions (such as uniform fluid pressure distribution on fractures), these models have been validated for distinct seismic events (Galis et al. 2017; Ji and Wu, 2020). The model proposed by McGarr (2014) is used to estimate the maximum seismic moment

$$M_0(\text{max}) = G\Delta V \quad (7-2)$$

where G and ΔV are the modulus of rigidity for reservoir rocks and injected volume, respectively. Here G represents the combined rigidity of the test system and the fracture (4.2 GPa) according to the method proposed by Ji et al. (2019).

The model from Galis et al. (2017) can predict the maximum self-arrested moment

$$M_0(\text{max-arr}) = \gamma \Delta V^{3/2} \quad (7-3)$$

where γ is the reservoir parameter and defined as

$$\gamma = \frac{0.4255}{\sqrt{\Delta\tau}} \left(\frac{\kappa\mu_d}{h} \right)^{3/2} \quad (7-4)$$

where κ is the bulk modulus of reservoir rocks and estimated as 2/3 of the combined rigidity (2.8 GPa) (Landau et al., 1960); h is the reservoir thickness and assessed as the ratio of the sample volume to fracture area (0.05 m) (Galis et al., 2017); $\Delta\tau$ and μ_d are the shear stress drop and dynamic friction coefficient, respectively (Table 7-1). Here the shear stress drop is the difference between the critical shear stress and minimum shear stress during fracture slip (Figure 7-2 and Figure 7-3). The dynamic friction coefficient is the ratio of the minimum shear stress during dynamic fracture slip to effective normal stress (Acosta et al., 2018). As the effective normal stress is nonuniform on the fracture, the mean fluid pressure over the fracture is estimated by averaging the injection and monitoring pressures at fracture failure and

the effective normal stress is then obtained by subtracting the average fluid pressure from the applied normal stress. Here γ is thus calculated as 2×10^{12} .

The seismic moment release of the injection-induced fracture slip in this study is almost purely seismic (Figure 7-2 and Figure 7-3). The coseismic slip is converted into seismic moment based on the definition of seismic moment (Aki, 1966)

$$M_0 = GAd \quad (7-5)$$

where A is the fracture area, and d is the slip displacement (Table 1). Because the length of the sawcut fracture is far smaller than the critical nucleation length of fracture in granite (McLaskey and Yamashita, 2017), the laboratory-scale sample cannot fully accommodate a fault rupture.

Figure 7-5 shows the seismic moment release of injection-induced fracture slip in the tests in this study as a function of the injected volume. The upper bounds of the models suggested by McGarr (2014) and Galis et al. (2017) are constructed with the combined rigidity of the test system and the fracture (4.2 GPa) and laboratory-scale reservoir parameter (2×10^{12}), respectively. The seismic moment release is calculated using Eq. (7-5) with the same combined rigidity. All of the data points in this study are below the upper bounds, and the seismic moment release increases linearly with larger injected volume under each normal stress. The injected volume and seismic moment release in the cyclic injection tests are lower than those in the monotonic injection tests under both the pressure-controlled and volume-controlled conditions. In the pressure-controlled cyclic injection tests, fluid depressurization results in the largest reduction in seismic moment release, which could be achieved by decreasing cumulative fluid volume to reduce the seismic risks in the field (Scanlon et al., 2019). Although lowering the critical injection pressure reduces the degree of fluid pressure heterogeneity (Figure 7-4), the seismic moment release in the cyclic injection test aCV3 is surprisingly comparable to that in the monotonic injection test aMV. This is presumably because the long-term injection process introduces extra fluid into the fracture and the frictional healing enhances the asperity contacts on the fracture (Im et al., 2017). The cyclic injection test aCP2 also shows frictional healing caused by the long-term fluid injection (Figure 7-2c).

Hence, the critical injection pressure should not be set much lower than the predict pressure at fracture failure, and needs to be optimized to avoid the increase in seismic moment due to overtime fluid injection.

Figure 7-5 Seismic moment as a function of injected volume. Red and blue lines denote the models suggested by McGarr (2014) constructed with a modulus of rigidity (G) of 4.2 GPa and by Galis et al. (2017) with a reservoir parameter (γ) of 2×10^{12} , respectively. Monotonic and cyclic fluid injection tests are marked by solid and open points, respectively.

7.4.2 Energy budget in cyclic fluid injection

The energy budget of injection-induced seismicity can be assessed using the relationship between seismic energy and hydraulic energy to anticipate the seismic energy release due to fluid injection (De Barros et al., 2019; Gómez Alba et al., 2020; Goodfellow et al., 2015). The seismic energy (E_r) released from a seismic event can be calculated based on an empirical relationship (Goodfellow et al., 2015; Kanamori, 1978),

$$\log(E_r) = 1.5M_w + 4.8 \quad (7-6)$$

where M_w is the moment magnitude and estimated as (Hanks and Kanamori, 1979),

$$M_w = \frac{\log M_0}{1.5} - 6.07 \quad (7-7)$$

The hydraulic energy (E_h) is determined as the input energy during fluid injection (Goodfellow et al., 2015),

$$E_h = \int_{t_1}^{t_2} P q dt \quad (7-8)$$

where P and q are the injection pressure and injection rate at time t , respectively. In the field, the seismic energy can be released from a fully developed rupture and perturbation of adjacent fault patches. The seismic energy estimated in this study is thus underestimated and serve as a lower boundary.

Figure 7-6a summarizes the relationship between seismic energy and hydraulic energy of monotonic and cyclic injection tests in various experimental, numerical and field cases. The seismic energy shows a tendency to increase with larger hydraulic energy. The ratio of the seismic energy to hydraulic energy is plotted and known as the seismic injection efficiency (Maxwell, 2013). The field injection tests in Äspö Hard Rock Laboratory and the numerical simulations of fluid injection into Soultz-sous-Forêts granite show that the evolution of seismic energy can be directly correlated to hydraulic energy during cyclic fluid injection (Kwiatek et al., 2018; Zang et al., 2017; Zang et al., 2013). For the tests in this study, the seismic injection efficiency ranges from $1.3 \times 10^{-5}\%$ to $1.3 \times 10^{-1}\%$, which coincides roughly with the typical ranges of the field cases associated with fracture activation ($1.0 \times 10^{-5}\%$ to $1 \times 10^{-1}\%$) (Maxwell, 2013). The zoomed view highlights that the seismic energy from the cyclic injection tests is lower than that from the monotonic injection tests (Figure 7-6b), which is consistent with the numerical and field observations. The hydraulic energy from the cyclic injection tests in this study is also smaller than that from the monotonic injection tests, in contrast to the numerical and field cases. In the field injection tests, the excessive hydraulic energy is due to fluid leak-off into adjacent fracture networks. However, the fluid pressure diffusion occurs primarily along the fracture in the tests in this study, and no obvious fractures are observed in

the granite matrix after the tests. Thus, the effectiveness of cyclic fluid injection is also related to the unstrained and drained conditions of fractured reservoirs.

(a)

(b)

Figure 7-6 (a) Seismic energy as a function of hydraulic energy. The data of Äspö field injection tests and the corresponding numerical simulations are obtained from Kwiatek et al. (2018) and Zang et al. (2013; 2017). Black dashed lines represent the seismic injection efficiency in percentage. (b) Zoomed-in view on the data in this

study. Monotonic and cyclic fluid injection tests are denoted by solid and open points, respectively.

7.4.3 Seismic risk mitigation using cyclic fluid injection

This study demonstrates that cyclic fluid injection into locally undrained fractures in nearly impermeable rocks decreases the degree of fluid pressure heterogeneity on the fractures, and reduces the seismic moment release if the critical injection pressure is well controlled to limit the injected volume and operation duration. Nevertheless, for fully drained fractures in permeable rocks, cyclic fluid injection promotes the occurrence of unstable slip due to the increase in critical fracture stiffness, but the reduction in critical injection pressure restricts the unstable slip (Noël et al., 2019). Both locally undrained and fully drained fractures are possibly encountered in fractured reservoirs, so different fracture responses challenge us to mitigate the seismic risks from complex fracture systems.

Cyclic fluid injection is believed as an effective treatment protocol to reduce the seismic risks associated with hydraulic stimulation. In the cyclic injection tests in Äspö Hard Rock Laboratory, the breakdown pressure and maximum magnitude from cyclic fluid injection are lower than those from the monotonic fluid injection (Kwiatek et al., 2018; Zang et al., 2017; 2018). Cyclic fluid injection combined with the traffic light system has successfully maintained the maximum magnitude below the target threshold of Mw 2.0 in the Pohang EGS project (Hofmann et al., 2019; Hofmann et al., 2018). Both hydro-fracturing and hydro-shearing are involved in the field fluid injection tests (Jalali et al., 2018), but play different roles in the occurrence of the seismic events. Laboratory fluid injection tests separately investigate the two factors, and demonstrate that hydro-fracturing dominates the injection-induced failure of intact rocks and releases the seismic energy to create fracture networks (Patel et al., 2017; Zhuang et al., 2019). In the tests in this study, when hydro-shearing governs the injection-induced failure of fractured rocks, cyclic fluid injection promotes the fluid pressure diffusion and reduces the seismic energy release.

Therefore, cyclic fluid injection can be further developed to modulate hydro-fracturing and hydro-shearing in different hydraulic stimulation stages, depending on operational requirements and monitoring feedbacks. Cyclic fluid injection with high critical injection pressure and high injection frequency could predominately improve the effectiveness of hydraulic fracturing. After that, both the critical injection pressure and injection frequency could be reduced to mainly enhance the permeability of natural and hydraulically induced fractures. Meanwhile, dominating fractures, either locally undrained or fully drained fractures, should be considered in determining the critical injection pressure. Particularly, if the maximum magnitude approaches a target threshold, the critical injection pressure and injection frequency must be carefully tailored to mitigate the seismic risks (Kwiatek et al., 2018; Hofmann et al., 2019). In future, it is beneficial to jointly discuss asperity interaction and failure along fractures (mechanical instability) and the analogue of high-pressure patches hydraulically communicating along fractures (hydraulic instability).

7.5 Summary

Fluid injection tests on locally undrained fractures in low-permeability granite were conducted to provide insights into cyclic fluid injection as a solution for seismic risk mitigation. The results show that cyclic fluid injection promotes the fluid pressure diffusion on the fractures and reduces the degree of fluid pressure heterogeneity, resulting in the reduction in seismic moment release. If the critical fluid pressure is over-reduced, the seismic moment release may not decrease significantly due to extra fluid injection and long-term frictional healing. The hydraulic energy in the cyclic injection tests on the locally undrained fractures is lower than that in the monotonic injection tests, in contrast to the observation in previous numerical and field cases possibly containing both locally undrained and fully drained fractures. The data indicate that determining a proper critical fluid pressure is essential to improve the performance of cyclic fluid injection. However, it is challenging to achieve the optimal scenario with minimal seismic moment release, as the other cycle-related factors (e.g., injected volume and operation duration) should also be tailored to balance the energy budget. Cyclic fluid injection

could be further improved with the considerations of special needs in stimulation stages and dynamic responses of fractured reservoirs, in terms of determination of dominating fracture types, modulation of hydro-fracturing and hydro-shearing, and comprehensive response to monitoring feedbacks.

CHAPTER 8 CONCLUSIONS AND FUTURE WORK

8.1 Conclusions

A series of experimental and numerical investigations has been conducted to study the shear-flow characteristics of rock fractures. Some underlying mechanisms of injection-induced fracture instability have been uncovered, which provide significant implications for injection-induced seismicity. The laboratory tests are performed using the triaxial shear-flow setup, which is properly designed and systematically corrected for the simulation of injection-induced instability of critically stressed rock fractures. The reliability of the triaxial shear-flow setup is well demonstrated by measuring the frictional strength of sawcut fractures in granite. The numerical recovery of fluid pressure diffusion process is achieved by solving the classic diffusion equation using COMSOL. The numerical modeling of injection-driven shear test is implemented using the hydro-mechanical coupled discrete particles joints model based on PFC2D.

First, the test results show that the injection-induced fracture instability is strongly dependent on fluid pressure heterogeneity and fracture surface heterogeneity. On the one hand, a high degree of fluid pressure heterogeneity in the sawcut fracture causes fluid pressure amplification near the injection boreholes, leading to that the maximum moment release exceeds the upper bounds predicted by McGarr (2014) and Galis et al. (2017). The instability of the natural fracture is due to the near-uniform reduction of effective normal stress associated with a low degree of fluid pressure heterogeneity. Fluid pressure heterogeneity strongly influences the fracture instability. On the other hand, the sawcut fracture exhibits dynamic slip with thermal pressurization, while the natural fracture experiences quasi-dynamic sliding with stress imbalance between the fixed shear stress and decreasing frictional strength. Fracture surface heterogeneity controls the seismic and aseismic behaviors of the fractures when the instability occurs. The experimental data also indicate that the moment release based on injected volume is predictable in the case with a low degree of fluid pressure heterogeneity. However, the current models are not suitable for the case with a high degree of fluid pressure heterogeneity, which may induce the fracture slip propagating beyond the stimulated reservoir.

Second, the test results also show that the fluid pressure at fracture failure can be predicted by the Mohr-Coulomb failure criterion when the degree of fluid pressure heterogeneity is low, while the fluid pressure at fracture failure is underestimated when the fluid pressure is highly heterogeneous. The COMSOL-based numerical results recover the evolution of fluid pressure distribution on the fracture and indicate that the failure mode of the fracture is dependent on the degree of fluid pressure heterogeneity. The fluid pressure at fracture failure increases with higher degree of fluid pressure heterogeneity, and the fracture slip extends to activate the entire fracture. The numerical results obtained from PFC2D modeling show that fluid pressure distribution strongly influences the nucleation and propagation of fracture slip. For non-uniformly pressurized fractures, dynamic slip can be driven by shear stress imbalance, run faster than pressurized fluid, and finally extend into unpressurized fracture area. The resulting seismic moment release is thus larger than that predicted by the existing models for uniformly pressurized fractures. It is suggested that besides injected volume, fluid pressure distribution should also be considered to predict the seismic moment release of non-uniformly pressurized fractures.

Finally, the optimization of fluid injection strategy to mitigate seismic risks is inspired by the in-depth understanding of the mechanisms of injection-induced seismicity. Cyclic fluid injection promotes the fluid pressure diffusion on the fractures and reduces the degree of fluid pressure heterogeneity, resulting in the reduction in seismic moment. If the critical fluid pressure is over-reduced, the seismic moment may not decrease significantly due to extra fluid injection and long-term frictional healing. The hydraulic energy in the cyclic injection tests on the locally undrained fractures is lower than that in the monotonic injection tests, in contrast to the observation in previous numerical and field cases possibly containing both locally undrained and fully drained fractures. The data indicate that determining a proper critical fluid pressure is essential to improve the performance of cyclic fluid injection. However, it is challenging to achieve the optimal scenario with minimal seismic moment, as the other cycle-related factors (e.g., injected volume and operation duration) should also be tailored to balance the energy budget. Cyclic fluid injection could be further improved with the considerations of special

needs in stimulation stages and dynamic responses of fractured reservoirs, in terms of determination of dominating fracture types, modulation of hydro-fracturing and hydro-shearing, and comprehensive response to monitoring feedbacks.

8.2 Future work

This thesis has advanced the basic understanding on physical controls of injection-induced seismicity through a series of experimental, analytical and numerical efforts. Building upon the main findings of this study, some recommendations for future work are proposed as follows to further constrain the physical processes controlling injection-induced seismicity and to reduce the seismic risks associated with fluid injection.

1. Natural faults are always characterized by spatially heterogeneous frictional and hydraulic properties, which may thus exhibit much more complex behavior in terms of slip mode and permeability evolution (Cappa et al., 2019; Guglielmi, Cappa, et al., 2015). Therefore, future studies considering the injection-induced behavior of a “nonuniform” fracture plane is necessary, which should be composed of at least two different patches each characterized by different roughness and/or minerals.

2. This study focuses on injection-induced behavior of favorably oriented fracture, which provides the lower limit to fluid overpressure. Although favorably oriented fracture is the most frequent and susceptible source of seismicity, fluid overpressure also has the potential to activate unfavorably oriented fracture under regional stress state because of its weakness compared to host rocks. The injection-induced behavior of unfavorably oriented fracture should be significantly different from favorably oriented fracture due to physical processes such as stress rotation (Giorgetti et al., 2019) and melt welding (Hayward and Cox, 2017). Therefore, future work can be carried out to study the injection-induced activation of unfavorably oriented fracture.

3. Acid stimulation is a promising method to improve the well productivity in deep geothermal reservoirs while maintaining a low seismic rate through injecting

acidified water. This has been proved in the Soultz-sous-Forêts EGS field, France (Calò et al., 2014; Lucas et al., 2020). However, the hydro-chemo-mechanical mechanism associated with acid stimulation remains poorly understood. Future investigation on fracture behavior driven by acidified water injection may be able to provide some valuable insights into the mitigation of seismic risks using acid stimulation.

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APPENDIX A CHARACTERIZATION OF BUKIT TIMAH GRANITE

Bukit Timah granite used in this study is a Middle Triassic igneous rock, mainly underlying the central Singapore island and covering approximately one-third of the island area (Zhou and Zhao, 2016). The average bulk density, grain density, water content and porosity of the granite are determined as 2660 kg/m³, 2670 kg/m³, 0.07% and 0.26%, respectively, based on the test standards suggested by International Society for Rock Mechanics (Franklin, 1979). X-ray diffraction (XRD) analysis shows that the granite mainly consists of 62% feldspar, 32% quartz, 5% biotite and 1% orthoclase. The following contents include the details of microscopical observation, as well as the determination of mechanical and hydraulic properties of Bukit Timah granite.

A.1 Microscopical observation

The microscopic observation of Bukit Timah granite matrix is conducted using a Scanning Electron Microscope (SEM). Figure A-1a exhibits limited cracks and pits on virgin granite matrix. At a closer look, a series of cavities (i.e. various cracks and irregular pores) scatters over the whole microscopic view (Figure A-1b). Cracklike cavities appear mainly at grain boundaries (e.g., (a), (c), and (e) in Figure A-1b), while intragranular cracks are very rare (e.g., (b) in Figure A-1b). The ends of long cracks are normally blunt (e.g., (d) in Figure A-1b), formed by bridging of two long cavities (Brace et al., 1972). These cavities, although tiny in dimensions, increase the compressibility of granite matrix (Adams and Williamson, 1923).

Figure A-1 SEM images of Bukit Timah granite.

A.2 Mechanical properties

A series of triaxial compression tests has been conducted using MTS 815 triaxial rock mechanics test system to obtain the mechanical properties of Bukit Timah granite. The deviator force and confining pressure are measured with an accuracy of $\pm 0.3\%$. The axial and circumferential strains are measured accurately and directly using a dual averaging axial extensometer (Figure A-2a) and a circumferential extensometer (Figure A-2b), respectively. The dual averaging axial extensometer considers the possible nonuniform deformation of the sample by measuring the deformation at two opposite locations. The end effect from the loading platen is excluded by measuring only the deformation of the middle half of the sample. The circumferential extensometer measures the distance between the two ends of the chain rather than the change of sample circumference, therefore, the following equation is used to calculate the change in sample circumference (ΔC) based on the output of the extensometer (MTS manual, 2004),

$$\Delta C = \frac{\pi(l_{cf} - l_{ci})}{\sin(\frac{\theta_{si}}{2}) + (\pi - \frac{\theta_{si}}{2})\cos(\frac{\theta_{si}}{2})} \quad (A-1)$$

where $l_{cf}-l_{ci}$ is the change in chord length measured directly by the extensometer, θ_s is the angle subtended by the chord length l_c , i and f stands for the initial and final states, respectively (Figure A-3). It is also noted that, the change in sample circumference calculated by Eq. (A-1) should also consider the jacket thickness (1 mm).

The chain length (l_c) does not change during the test, therefore (MTS manual, 2004),

$$\theta_{si} = 2\pi - \frac{l_c}{R_i + r_r} \quad (A-2)$$

where R_i and r_r are the initial radius of sample and the radius of roller, respectively.

The volumetric strain (ε_v) can be calculated from the axial strain (ε_1) and circumferential strain (ε_3) as (Brace et al., 1966),

$$\varepsilon_v = \varepsilon_1 + 2\varepsilon_3 \quad (A-3)$$

The volumetric compaction, axial compression and circumferential expansion are signed by positive volumetric strain, positive axial strain, and negative circumferential strain, respectively.

Figure A-2 Schematic diagram of (a) dual averaging axial extensometer and (b) circumferential extensometer. (after MTS manual, 2004).

Figure A-3 Diagram of the working principle of the circumferential extensometer. (after MTS manual, 2004).

The typical stress-strain curves obtained from the triaxial compression test is shown in Figure A-3, which are produced from a triaxial compression test conducted under 30 MPa confining pressure. First, the confining pressure is manually increased to 30

MPa and then kept constant during the subsequent axial compression. Second, the sample is loaded by increasing the deviator force at 0.5 kN/s until 300 kN. Third, the load control mode is automatically changed into circumferential strain-control mode at a rate of 0.001 mm/s until the complete failure of the sample. The post-peak behavior is successfully obtained and exhibits a typical class II failure characteristic.

The tangent lines of the linear portions of the three curves are l_1 , l_2 and l_3 . The slope of l_1 is the Young's modulus, and the ratio between the slopes of l_1 and l_2 is the Poisson's ratio. At point p_1 , where the nonelastic behavior starts, the sample volume begins to increase relative to elastic changes. This is due to the dilatancy of rock fractures (Brace et al., 1966). At point p_2 , the volumetric compaction of the sample reaches the maximum. The photos of extensometers clamped on the sample are shown in Figure A-4b. The sample shows a Y-shaped failure with two conjugate shear fractures (Figure A-4c).

The peak axial stress as a function of confining pressure (10, 15, 20 and 30 MPa) is shown in Figure A-5a. The linear regression of these four points yields a linear function between the peak axial stress and confining pressure. The function can be substituted into Eq. (2-6) to obtain the cohesion and internal friction angle of the granite, which are 38 MPa and 54° , respectively. The Young's moduli and Poisson's ratios obtained from a series of triaxial/uniaxial compression tests are plotted against the confining pressure in Figure A-5b. The Young's modulus increases with the confining pressure at a rate as small as 0.02 GPa/MPa. The average Poisson's ratio decreases with confining pressure at a rate as small as 0.00037/MPa. The average Young's modulus is 73.8 GPa with a standard deviation of 0.88 GPa. The average Poisson's ratio is 0.16 with a standard deviation of 0.0061.

(a)

(b)

(c)

Figure A-4 Results of triaxial compression test. (a) Deviator stress as a function of axial, circumferential and volumetric strains. (b) Extensometers clamped on the sample. (c) Failure mode of the sample after the test.

Figure A-5 (a) Mohr-Coulomb failure criterion of the granite. (b) Young's modulus and Poisson's ratio of the granite as a function of confining pressure.

A.3 Hydraulic property

The permeability of Bukit Timah granite is also quantified. A granite sample containing no visual flaws such as fractures is selected for permeability measurements. The intact and macroscopically homogeneous sample with a height

of 50.20 mm and a diameter of 50.50 mm is vacuum saturated with distilled water for 24 hours before measuring its permeability (Figure A-6).

Figure A-6 Sample saturation before permeability measurement.

Figure A-7 Permeability of granite matrix under various combinations of confining pressures and pore pressures.

To determine the effective stress law for permeability of the granite, permeability measurements are conducted under various combinations of confining pressures and pore pressures (Figure A-7). Specifically, the confining pressure is increased step wisely from 7 MPa (Step 1), 14 MPa (Steps 2, 3 and 4), to 21 MPa (Step 5). For 7 MPa and 21 MPa confining pressures, the permeabilities are measured under 1 MPa pore pressure, while permeabilities under 3 MPa, 2 MPa and 1 MPa pore pressures

are measured under 14 MPa confining pressure. In the last step, the permeability is measured again at an effective stress of 13 MPa to examine the permeability recovery after a higher effective stress of 20 MPa.

Because the extremely low permeability of the granite, the pulse-decay technique has been employed (Brace et al., 1968). This technique records the evolution of differential pressure between two ends of the sample with elapsed time after a pore pressure step is imposed. The measurements are conducted using MTS 815.02 MTS 815 triaxial rock mechanics test system incorporating MTS 286.20 confining pressure system, and MTS 286.31 transient pulse pore pressure system. The differential pressure transducer in the permeability system can record the pressure with an uncertainty of $\pm 0.3\%$. An example of the recording differential pressure during a pulse-decay permeability measurement is shown in Figure A-8. The natural log of differential pressure from ΔP_i to ΔP_f follows a linear trend with time Δt . The slope of the fitted line $\ln(\frac{\Delta P_i}{\Delta P_f}) / \Delta t$ is related to the permeability of sample, the size

of the sample and reservoirs, and to the fluid properties such as viscosity and compressibility. The permeability can be calculated using Eq. (2-45). In this system, reference volumes are the same, i.e., $V_1 = V_2 = V$, so Eq. (2-45) can be simplified as

$$k = \frac{\mu_f \beta_c V L_s (\ln(\frac{\Delta P_i}{\Delta P_f}) / \Delta t)}{2 A} \quad (A-4)$$

where μ_f is the fluid viscosity, β_c is the fluid compressibility, V is the reference volume (332 cm^3), L_s is the sample length, $\ln(\frac{\Delta P_i}{\Delta P_f}) / \Delta t$ is the slope of the fitted line for the natural log of differential pressure vs. time, and A is the cross-sectional area of the sample.

The reference volume is 332 cm^3 in this system. The fluid circulating in this system is distilled water with μ_f and β_c equal to $0.001 \text{ Pa}\cdot\text{s}$ and $453 \times 10^{-12} \text{ Pa}^{-1}$, respectively.

Although distilled water may exert additional effects to rock properties, the dominant effect on permeability is stress change.

Figure A-8 Typical test result of a pulse-decay measurement.

(a)

(b)

Figure A-9 Determination of effective stress coefficient. The effective stress coefficient is the ratio of the two slopes.

The ratio of slope method is used to determine the effective stress coefficient according to the following equation (Kwon et al., 2001),

$$\chi = -\frac{\partial \log k / \partial P_p}{\partial \log k / \partial P_c} \quad (\text{A-5})$$

The coefficient obtained by Eq. (A-5) quantifies the susceptibility of permeability to pore pressure.

For this measured data set (Figure A-9), χ is 2.3 indicating that the permeability is more sensitive to the change of pore pressure than the change of confining pressure. It is also noteworthy that the permeability is unrecoverable after the effective stress reduces from a higher value (Figure A-7).

The exponential law (Gensterblum et al., 2015) of matrix permeability is used to mathematically depict the stress-dependence of granite permeability,

$$k(\sigma_{\text{eff}}) = k_0 \exp[-c_m (P_c - \chi P_p)] \quad (\text{A-6})$$

where σ_{eff} is the effective stress, k_0 is the intrinsic permeability at “zero” effective stress, and c_m is the stress sensitivity coefficient, respectively. Since $\chi=2.3$ in the harvested data, $\sigma_{\text{eff}} = P_c - 2.3P_p$.

The permeability of the granite as a function of effective stress is shown in Figure A-10, which decreases with higher effective stress. The intrinsic permeability of the granite is $1.3 \mu\text{D}$. The mechanical and hydraulic properties of the granite are listed in Table A-1.

Figure A-10 Permeability of Bukit Timah granite as a function of effective stress.

Table A-1 Mechanical and hydraulic properties of Bukit Timah granite.

Cohesion (MPa)	Internal friction angle (degree)	Young's modulus (GPa)	Poisson's ratio	Intrinsic permeability (μD)
38	54	73.8	0.16	1.3