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# **Hydromechanical modelling of pulse tests that measure fluid pressure and fracture normal displacement at the Coaraze Laboratory site, France**

F. Cappa (1) , Y. Guglielmi (1), J. Rutqvist (2), C.-F. Tsang (2), and A. Thoraval (3)

(1) UMR 6526 Géosciences Azur, CNRS-UNSA, Tectonics and Geomechanics Division, 250 rue A. Einstein, Sophia Antipolis, 06560 Valbonne, France

(2) Lawrence Berkeley National Laboratory, Earth Sciences Division, Berkeley, CA 94720, USA

(3) INERIS, Ecole National Supérieure des Mines de Nancy, Parc de Saurupt, 54042 Nancy, France

## **Abstract**

In situ fracture mechanical deformation and fluid flow interactions are investigated through a series of hydraulic pulse injection tests, using specialized borehole equipment that can simultaneously measure fluid pressure and fracture displacements. The tests were conducted in two horizontal boreholes spaced 1 m apart vertically and intersecting a near-vertical highly permeable fault located within a shallow fractured carbonate rock. The entire test cycle, both the initial pressure increase and subsequent pressure fall-off, was carefully monitored and used for the evaluation of the in situ hydromechanical behaviour. The field data were modelled using both distinct-element and finite-element modelling techniques, in two and three dimensions. Field test data were evaluated by plotting fracture normal displacement as a function of fluid pressure, measured at the same borehole. The curves for the normal displacement of the fracture as a function of fluid pressure showed hysteresis loops, in which the paths for loading (pressure increase) and unloading (pressure decrease) are different. By matching this behaviour, the fracture normal stiffness and an equivalent stiffness (Young's modulus) of the surrounding rock mass were back-calculated. Evaluation of the field tests by coupled numerical hydromechanical modelling showed that the pressure-increase path of the normal displacement-versus-pressure curve is highly dependent on the hydromechanical parameters of the tested fracture and the stiffness of the matrix near the injection point, whereas the pressure-decrease path is influenced by mechanical processes within a larger portion of the surrounding fractured rock.

**Keywords:** Numerical modelling; Pulse tests; Hydromechanical coupling; Distinct element method; Finite element method; Fracture networks

## **1. Introduction**

A particularly important feature in the interaction between mechanical deformation and fluid flow in fractured rock is its heterogeneity, both at the scale of the single fracture and the entire fracture network. However, complete and accurate description of the hydromechanical interactions within fractured rock is often difficult, largely because of the complexity of the fracture-network geometry and the heterogeneity in both fracture and rock-matrix hydromechanical properties.

The hydromechanical behaviour of fractured rock has been extensively studied through laboratory experiments on single fractures [1], [2], [3], [4], [5], [6], [7], [8], [9], [10], [11], [12], [13], [14], [15], [16], [17] and [18], field testing [5], [19], [20], [21], [22], [23], [24], [25], [26], [27], [28], [29], [30] and [31], and numerical simulations [32], [33], [34], [35], [36], [37], [38], [39], [40], [41], [42] and [43]. Most of the field studies have been conducted

at great depth in fractured hard rock, in which the permeability of the rock matrix is generally low and fractures act as dominating fluid conducting pathways. In the study presented in this paper, the main mechanisms for coupled hydromechanical processes are the mechanical deformation and fluid flow occurring within the fracture network. Hydromechanical coupled effects within one fracture depend not only on its hydraulic and mechanical properties, but also on the hydraulic and mechanical connections with other fractures, the orientation and magnitude of effective stresses applied to fracture walls, and general orientation (dip).

In a few previous studies, hydraulic well-testing has been applied to investigate in situ coupled hydromechanical effects in fractured rock [19] and [33]. Those studies indicate that hydraulic field tests can provide a good estimate for the hydromechanical properties of fractures. In tight, hard rock, single-borehole hydraulic pulse injections have been applied to determine hydraulic properties of rock fractures, including permeability and storativity [34], [36] and [38]. Using specialized equipment for short duration pulses, this method has been proven to be useful for measuring hydraulic properties in fractures located at several hundred metres depth, with hydraulic aperture values as small as a few microns [34] and [36].

Conventionally, the interpretations of a pulse test are based on matching the field results to type curves of pressure change with time, assuming either uniform linear, radial, or spherical flow, but without consideration of coupled hydromechanical mechanisms [45], [46], [47], [48], [49] and [50]. Due to the complex flow paths and fluid-flow-induced deformations in a fracture network, such simple assumptions about flow geometry and the role of hydromechanical coupling may not be valid. Coupled hydromechanical modelling has shown that the deformation of a fracture during a pulse-injection test can induce misinterpretations, leading to a significant error in the determination of fracture permeability and storativity [34]. To improve accuracy in estimating the hydromechanical properties of fractures, Rutqvist et al. [37] recommended combining hydraulic field tests and fracture-mechanical-normal-displacement measurements. Mechanical measurements of transient aperture change during a pulse injection test can provide a substantially improved estimate of fracture storativity, which implies that fracture permeability can be determined more accurately from the pressure transient test. Nevertheless, field tests on single fractures or a fracture network that measure simultaneously both hydraulic and mechanical responses are rare [21], [23], [27], [28], [29], [30] and [31].

The coupled hydromechanical behaviour of fractured rock has been studied in rock mechanics over the past 30 years, primarily in research programs associated with geological disposal of nuclear waste [51]. These research programs are concerned with flow in fractures at great depths, under high stresses and with a relatively small hydraulic aperture and high stiffness. Fracture hydromechanical behaviour under such conditions has been studied in several underground research laboratories. By contrast in the research that is the subject of this paper, hydraulic-pulse-test and hydromechanical measurements are carried out in a shallow fractured rock unit, the Coaraze Laboratory site located in southern France. The Coaraze medium-scale field experimental and modelling project [28] aims at understanding coupled groundwater and mechanical processes in complex, shallow, fractured and porous rock, in which fractures are highly permeable and well connected. This is an intermediate scale test site (30 m×30 m×15 m) for which the experimental conditions are relatively well controlled. Experiments conducted on that site explore mechanisms at a scale between laboratory and regional reservoir.

In this study, we combine simultaneous in situ measurements of fluid pressure and fracture-normal displacements during pulse-injection testing. A series of 70 pulse tests are conducted in two sub-horizontal boreholes intersecting a near-vertical fracture within the Coaraze Laboratory site. Fracture hydromechanical responses (fluid pressure and normal displacement) are simultaneously monitored at two measuring points spaced about 1 m apart within the plane of the vertical fracture.

We first present the experimental set-up and field test results, followed by coupled hydromechanical modelling of a pulse test using a distinct-element fracture network model, and a finite-element model of the pulse-tested fracture embedded in an equivalent poro-elastic medium. In both sets of numerical analysis, the observed normal displacement-versus-pressure responses are matched to back-calculate the in situ fracture hydraulic aperture and normal stiffness. We subsequently evaluate, in a sensitivity study, the influence of several key parameters on the normal displacement-versus-pressure responses, including various fracture and rock-matrix hydraulic and mechanical properties, as well as fracture-network geometry.

## **2. Field hydromechanical experiments**

### **2.1. Test site**

Field experiments were conducted at the Coaraze Laboratory site in France [28]. The site is a small fractured rock reservoir (30 m×30 m) in a surface outcrop made up of a 15 m thick pile of fractured limestone (Fig. 1a). The hydraulic boundaries of the reservoir are well constrained, with impervious layers at the bottom and top, an impervious fault located downstream toward the southwest, and an exposed rock slope surface sealed by semi-permeable grout to a height of about 10 m. Upstream, towards the northeast, a permeable fault allows water to continuously flow into the reservoir. The rock mass is naturally drained by a spring located in the southwest corner. For these experimental studies, the spring was artificially closed with a water gate that enabled us to control the piezometric level in the reservoir (by opening or closing the gate). Thus, when the gate is closed, no more discharge occurs at the spring, and the pressure increases and stabilizes up to 10 m above the gate. When the gate is opened, water stored in the reservoir flows out.

A three-dimensional deterministic model of the discontinuity network geometry was obtained from surface and boring scan-line data (Fig. 1b). The reservoir network is made up of 26 discontinuities, forming three sets (Fig. 1a and 1b):

- Three faults with a N50 to N70 trend, a dip angle of 70° to 90° towards the NW ( $F_{11}$ ,  $F_{12}$  and  $F_{13}$ ), and with a 2–3 m spacing;
- Eleven faults with a N120 to N140 trend, a dip angle of 75° to 90° towards the NE ( $F_1$ – $F_{10}$  and  $F_{14}$ ), and with a 2 m spacing;
- Twelve bedding planes with a N40 trend, a dip angle of 45° towards the SE ( $S_1$ – $S_{12}$ ), and with a 0.5–1 m spacing.

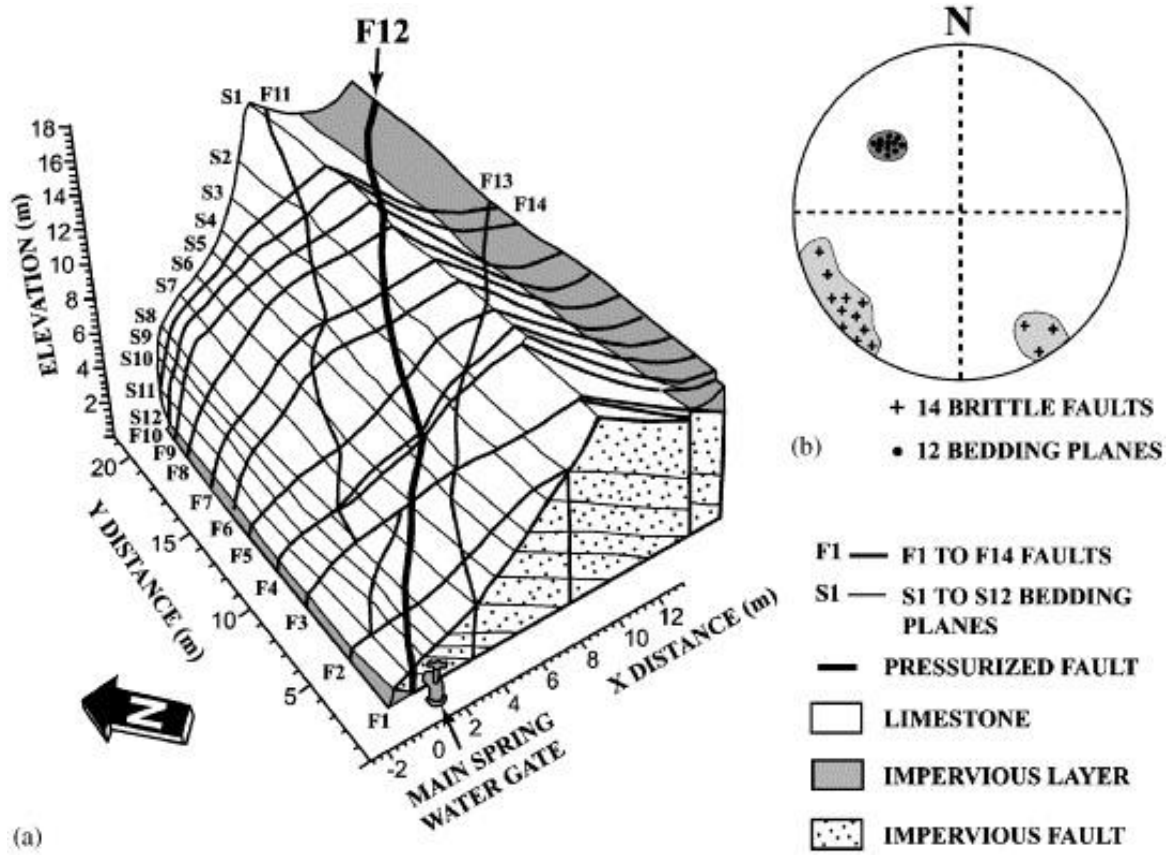


Fig. 1. (a) Three-dimensional view of the fractured rock mass at the Coaraze Laboratory Site; (b) stereonet of plots to discontinuities.

## 2.2. Hydromechanical behaviour of the fractured rock mass

Using various hydraulic field tests, including measurement during opening and closure of the water gate, Cappa et al. [28] and Guglielmi and Murdy [52] investigated the overall fluid flow and hydromechanical behaviour in the reservoir. Based on the fluid flow pattern and the time evolution of fluid flow and pressure responses, they found that the hydraulic responses at the site are governed by dual-permeability behaviour. Directly after opening or closing the water gate, transient flow occurs only in the highly permeable faults ( $0.57 \times 10^{-4} < K_F$  (fault permeability)  $< 1.9 \times 10^{-4}$  m/s), and later, when a steady-state flow is reached in faults, water flows from faults to low-permeable bedding planes ( $0.9 \times 10^{-6} < K_{BP}$  (bedding plane permeability)  $< 7.6 \times 10^{-6}$  m/s). The rock matrix between major faults and bedding planes is practically impervious, whereas connectivity between discontinuities (major faults and bedding planes) is high. Simultaneous measurements of pressure and fracture-normal displacement indicate a direct hydromechanical coupling in the highly permeable faults, where a pressure increase is directly accompanied by fault opening. No direct hydromechanical coupling occurs within the low-permeable zones, where deformation does not directly correlate with pressure increase. This means that mechanical deformation in the bedding planes and rock matrix is induced by the opening of adjacent highly permeable faults.

### 2.3. Experimental setup

Two horizontal boreholes (HM1 and HM2 in Fig. 2), spaced 1 m vertically, were drilled normal to the  $F_{12}$  near-vertical fault (Fig. 2). Each borehole terminates 5 cm beyond the fault. In each borehole, the fault was isolated with an inflatable packer to create a 0.4 m long sealed section. In each sealed section, the measurement device consists of a fibre-optic fluid pressure and a fibre-optic normal displacement sensor fixed to the borehole walls by two anchors located on both sides of the fault. This device was adapted from the BOF-EX device developed by RocTest-Telemac® [30] and [53]. This borehole equipment is capable of simultaneously measuring changes (with a high frequency [120 Hz] and high accuracy) in fluid pressure ( $\pm 1$  kPa) and displacement normal to the fault walls ( $\pm 1 \times 10^{-7}$  m) during a fast pulse test.

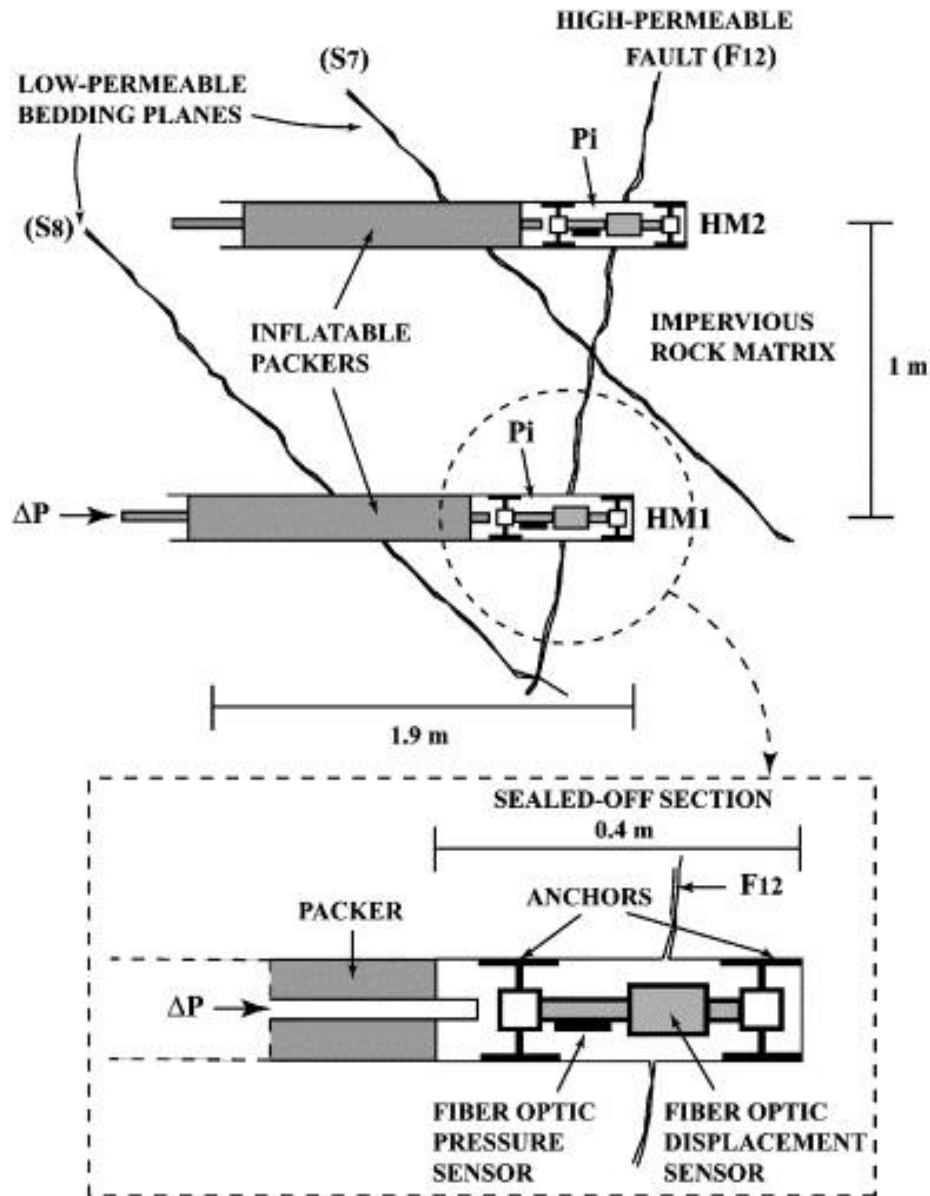


Fig. 2. Experimental device used for hydraulic pulse tests.

Before performing the pulse-test series, the spring-gate is closed in order to saturate the fracture network with water. Before each pulse test, initial pressure values are 39 kPa at HM1 and 27 kPa at HM2. The section sealed by the packer is connected to a valve leading to an upstream volume that is used to perform a pressure pulse test. To conduct the pulse tests, the fluid pressure is first increased upstream of the valve to the required pressure-pulse magnitude by a volumetric water pump, allowing a pressure increase from 10 to 700 kPa. Thereafter, the pulse is initiated when the valve is opened to allow water to enter inside the sealed section. The pressure first increases in the packed-off section, and then the valve is closed. After closing the water gate, the pressure starts to decrease as a result of fluid flow into the fault. In both HM1 and HM2, a series of seventy pulse tests were conducted at different pressure magnitudes ( $\Delta P=9$  to 86 kPa), repeated at least three times each.

### **3. Hydromechanical measurements**

Among the 70 pulse tests, results for a pulse test of magnitude equal to 86 kPa applied to HM1 are presented as an example of typical results (Fig. 3). Indeed, repeated for different pulse pressure, all the tests display the same curve shape. Data were sampled at a rate of 120 measurements per second with filtering of the instrumental noise. Fibre optic sensors allow accurate measurements, with a high sampling rate, of all the pressure changes during a short-duration pulse [30]. Thus, compared to the classical pulse curves for which only the pressure decrease stage is measured with sufficient resolution, fibre optic sensing allows interpretable field data for both pulse-pressure increase and decrease stages.

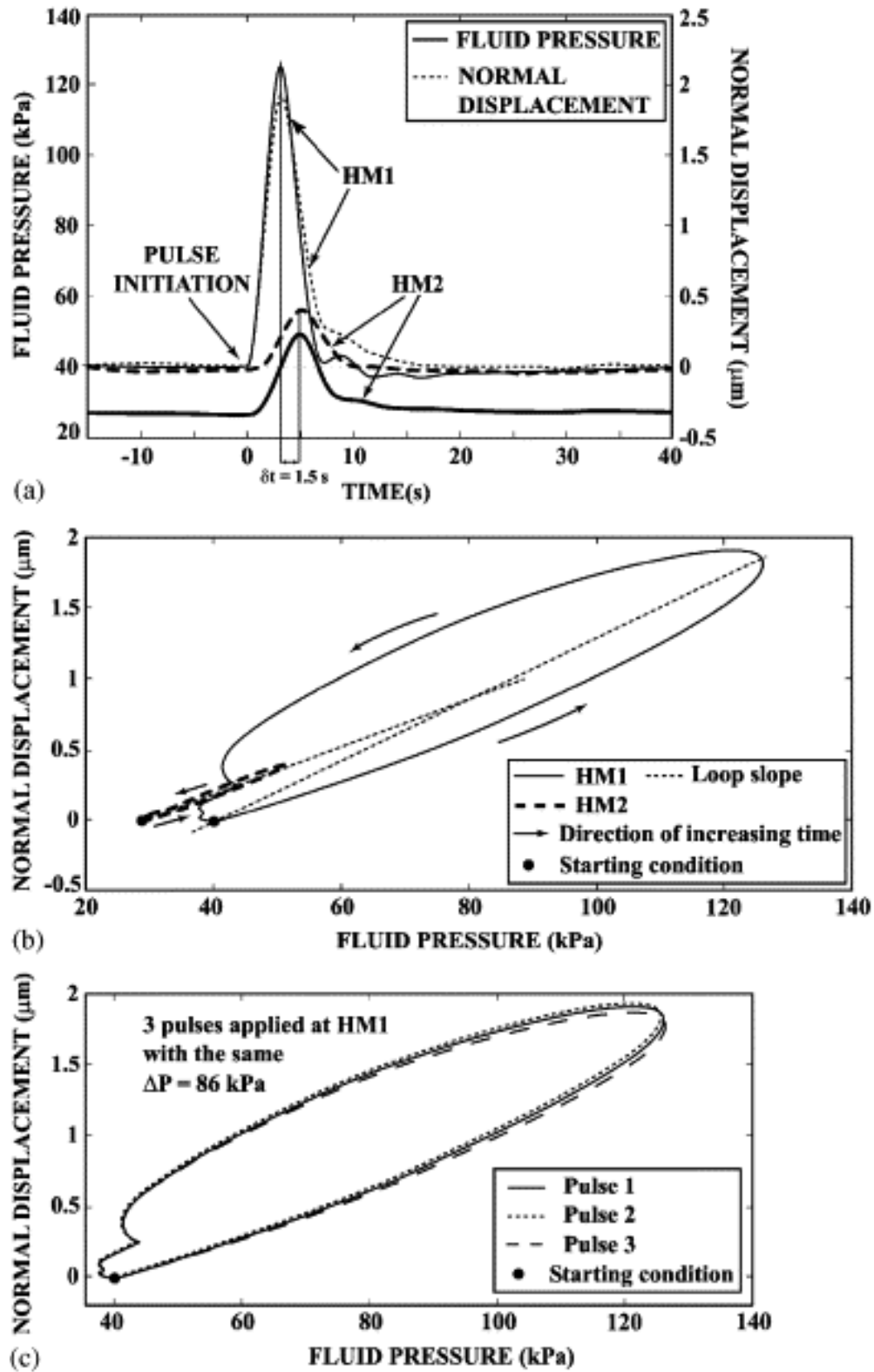


Fig. 3. Results of a pulse test applied to HM1: (a) pressure and normal displacement at points HM1 and HM2 as a function of time (the pressure prior to the test is of 39 kPa at HM1 and 27 kPa at HM2); (b) normal displacement as a function of pressure showing loop-like behaviour; (c) repeatability of the loop-like behaviour for three pulses of same magnitude ( $\Delta P = 86$  kPa) applied at HM1.



For a test duration of 40 s, the initiation of pulse ( $t=0$  in Fig. 3a) begins 15 s after the start of monitoring. At HM1, the pressure change displays a nonlinear oscillatory behaviour, with pressure increasing from 39 to 125 kPa in 3.5 s, and then decreasing to reach its initial value in 8 s (Fig. 3a). The normal displacement follows the pressure change, although the displacement is less oscillatory and more damped compared to pressure. Around the time of peak pressure, the magnitude of normal displacement in HM1 reaches a maximum of  $1.9 \times 10^{-6}$  m. The peak pressure at HM2 is lower than at HM1 ( $\Delta P=22$  kPa instead of 86 kPa) and is reached 1.5 s later. At HM2, the pressure decrease is slower than the one at HM1. Normal displacement change at HM2 follows the local pressure change at HM2, with the magnitude of displacement reaching a maximum of  $0.45 \times 10^{-6}$  m.

The same pulse test is presented in Fig. 3b in the form of normal displacement as a function of fluid pressure. Both at HM1 and HM2, the curves follow a loop-shaped evolution, along different paths for pressure increase and decrease stages. The loop-like behaviour indicates that pressure and normal displacement return to the initial starting point at the end of the pulse. During the pressure pulse evolution, the loop is regular, but near the return to ambient pressure, the shape of the loop becomes oscillatory. The higher the pulse magnitude, the thicker the loop. Results clearly indicate a difference of “loop” slopes at HM1 and HM2 (thin dashed lines between the initial conditions and the pressure-peak, in Fig. 3b).

Fig. 3c illustrates the repeatability of the loop-like behaviour showing three loops of same pulse magnitude ( $\Delta P=86$  kPa). The curves are superimposed by less than 1 kPa standard deviation in pressure and a less than  $2 \times 10^{-7}$  m standard deviation in displacement.

Fig. 4 presents a plot of maximal normal displacement-versus-maximum pressure and the best-fitting linear trend lines for the entire series of pulse tests. Data at HM1 and at HM2 follow two different linear trend lines, with an average linear slope of normal displacement-versus-fluid pressure data equal to  $2.34 \times 10^{-8}$  m/kPa at HM1 and  $1.81 \times 10^{-8}$  m/kPa at HM2. The inverse of these values corresponds to a first-order analytical estimate of an apparent fracture normal stiffness trend of about 43 and 55 GPa/m, at HM1 and HM2, respectively. Field data around these trend lines are scattered by less than 10 kPa standard deviation in pressure and a less than  $2 \times 10^{-7}$  m standard deviation in displacement. However, a closer analysis shows that displacement data for HM1 and HM2 approximately coincide for pulse magnitudes below 40 kPa, whereas above a pressure of 40 kPa, HM1 data tends upwards and HM2 data trends downwards (Fig. 4).

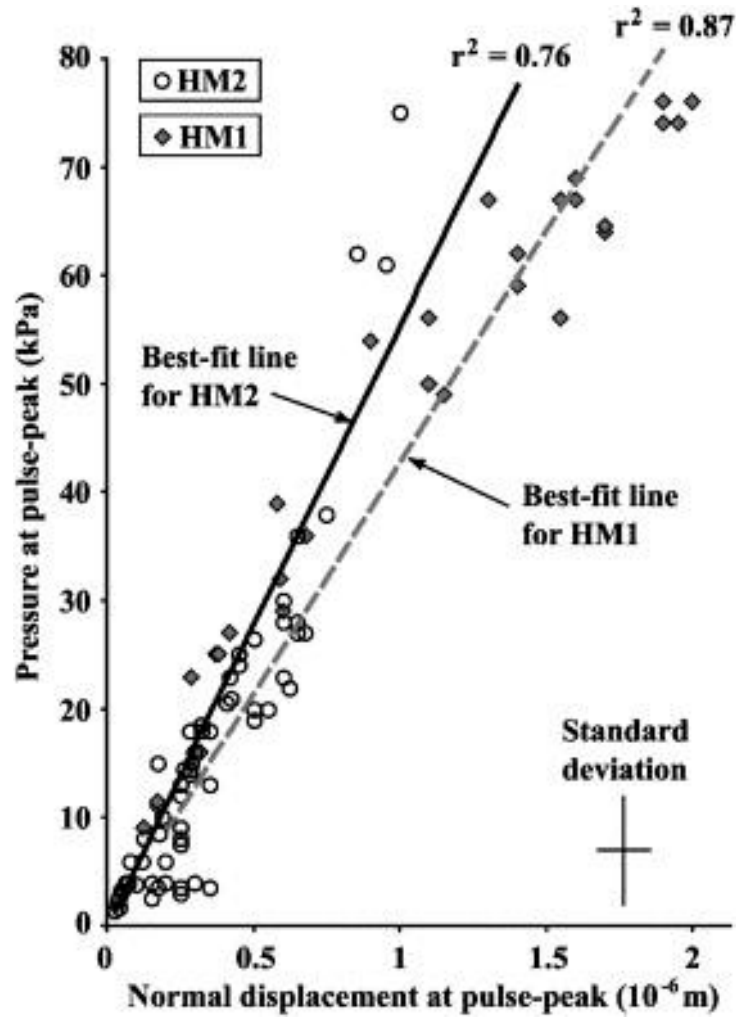


Fig. 4. Normal displacement as a function of pressure at pulse-peak for all 70 pulse tests.

#### 4. Coupled hydromechanical modelling of a pulse test

##### 4.1. Numerical analysis methods

Two numerical modelling approaches were used to simulate the above-mentioned pulse test of 86 kPa applied to HM1. These approaches account for heterogeneous hydromechanical interactions in a complex fracture network as well as fracture-matrix hydromechanical interactions. First, the finite element code ROCMAS [54] and [55] was used to investigate the effects of leakage into the rock matrix on the hydromechanical response. This code is based on Biot's theory of consolidation [56] for analysis of thermo-hydro-mechanical processes in saturated–unsaturated fractured porous rock media. In ROCMAS, a few fractures embedded in a permeable rock matrix can be discretised with discontinuity model elements embedded in solid model elements. Second, the UDEC and 3DEC distinct element codes [57], [58] and [59] were selected to investigate the effects of fracture network geometry on the hydromechanical response. These codes simulate the fluid flow and deformation-coupled processes of discrete fractures embedded in an impervious rock matrix. Thus, both modelling approaches are well suited to investigate the hydromechanical behaviour of a fracture, and complement each other.

In the numerical analyses conducted herein, discontinuities and the rock matrix are considered to be linear elastic. A modified form of the cubic law (1) governs fluid flow in fractures [44] and [60]:

$$q = \frac{(b_{hi} + f \Delta U_n)^3 w \rho g}{12 \mu} \nabla h, \quad (1)$$

where  $q$  is the flow rate,  $b_{hi}$  is the initial hydraulic aperture at the initial effective stress,  $f$  is a factor reflecting the influence of the roughness on the tortuosity of the flow,  $\Delta U_n$  is the change in fracture normal displacement,  $w$  is the fracture width,  $\rho$  is the fluid density,  $g$  is the gravitational acceleration,  $\mu$  is the fluid dynamic viscosity, and  $h$  is the gradient in hydraulic head. Fracture deformations and hydraulic apertures are calculated as a function of the effective stresses, assuming a constant normal stiffness for the fractures. The modelling procedure consists of three stages:

- The first modelling stage consists of a code comparison to check the capabilities and correspondence of the numerical simulation models. In this application, a pulse test was simulated by two- and three-dimensional axisymmetric models of a single horizontal fracture—2DA×M<sub>1</sub> (Fig. 5 and Fig. 6) and 3DA×M (Fig. 7a).

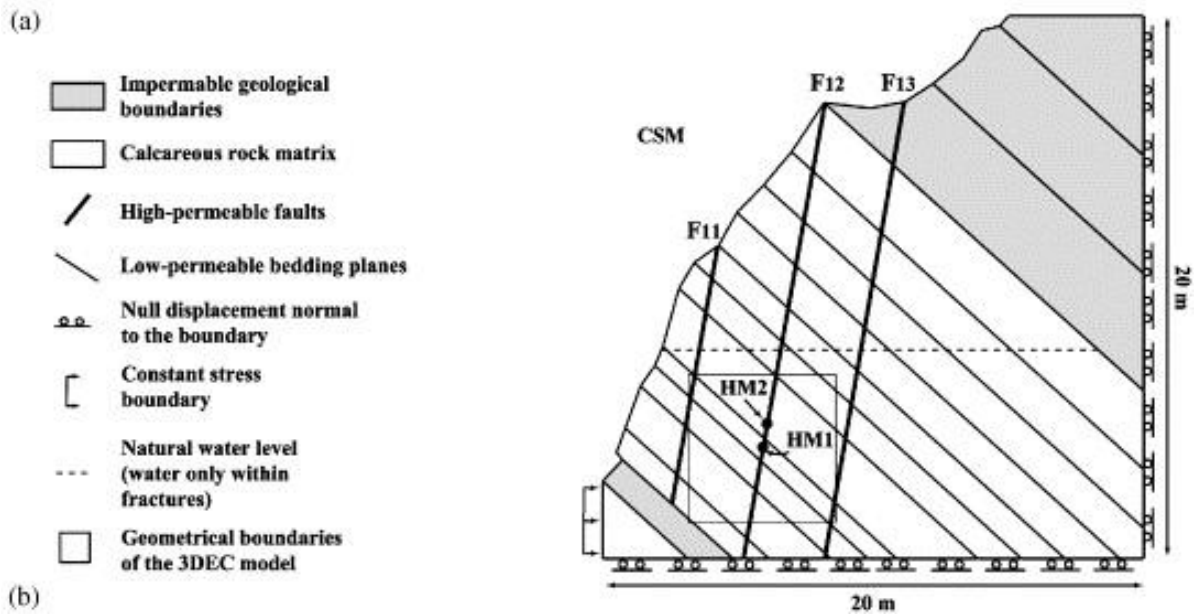
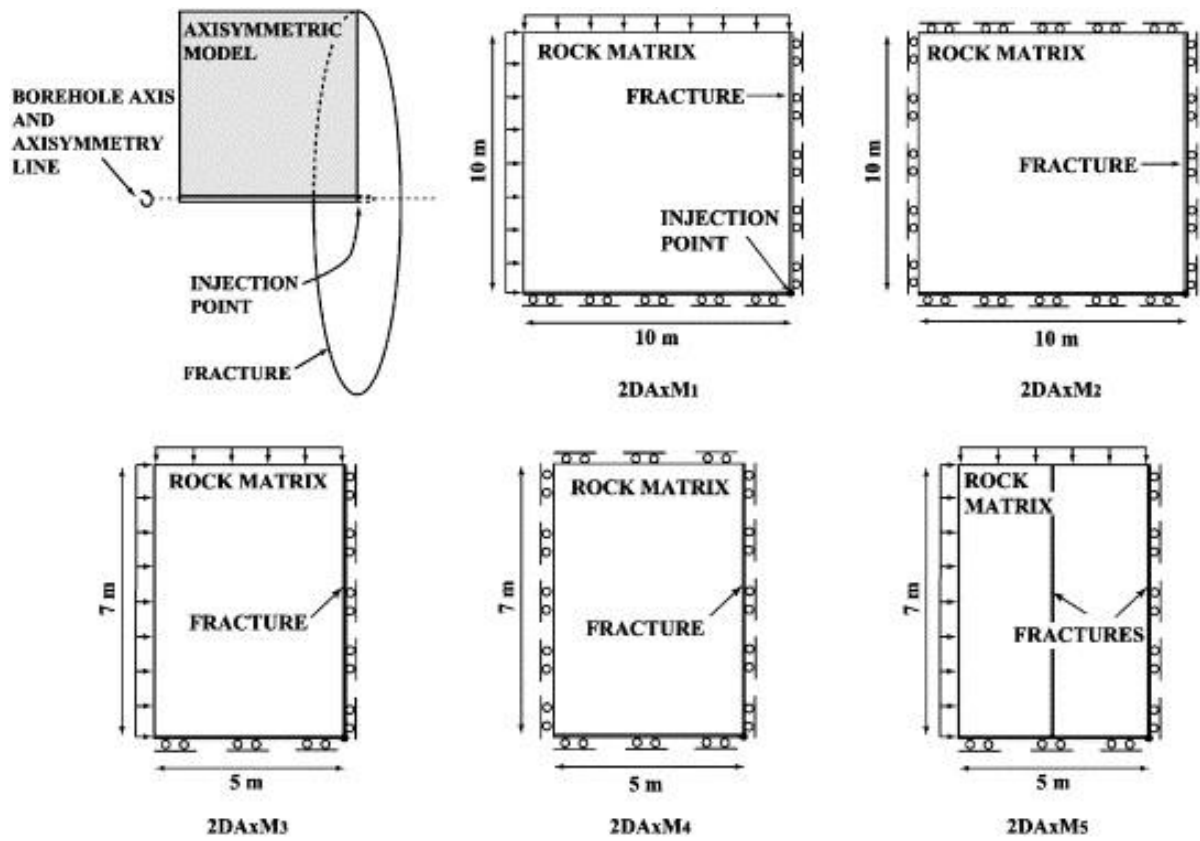


Fig. 5. Geometry and boundary conditions of the two-dimensional numerical models; (a) axisymmetric models of a single vertical fracture used for the code comparison, and the two-dimensional analysis (models 2DAxM<sub>1</sub>–2DAxM<sub>5</sub>); (b) full section of the Coaraze rock mass (model CSM).

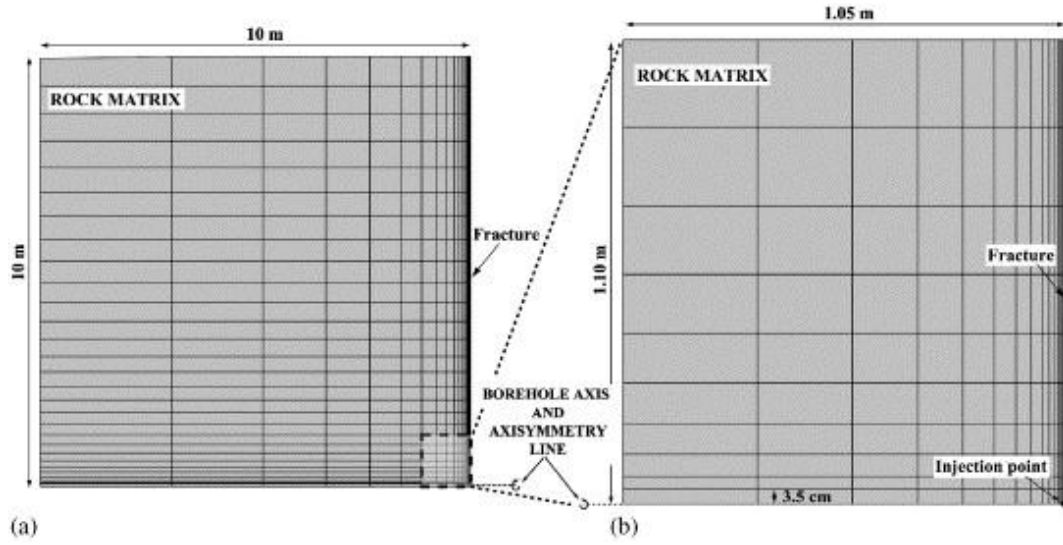


Fig. 6. Finite element mesh assigned to ROCMAS model: (a) entire model; (b) close-up vertical  $x$ - $z$  section around the injection point.

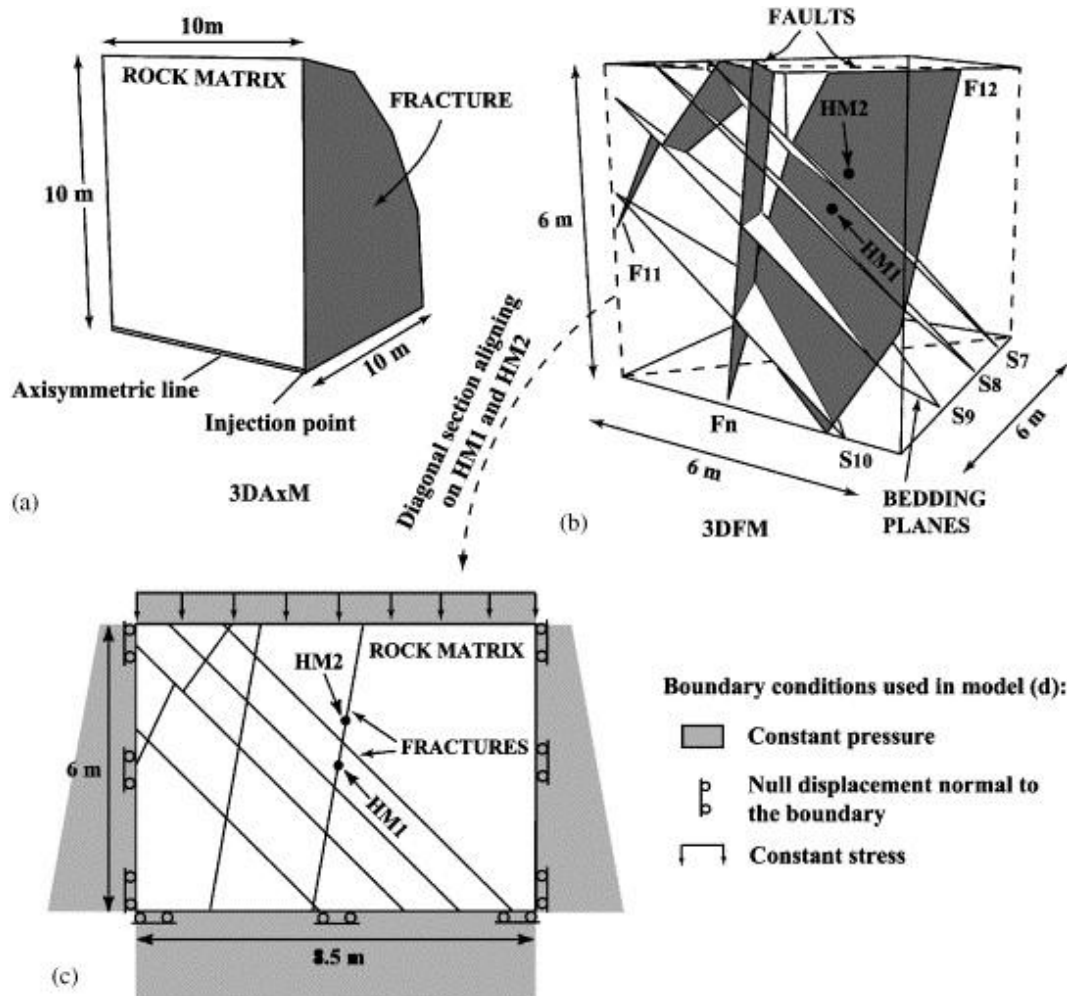
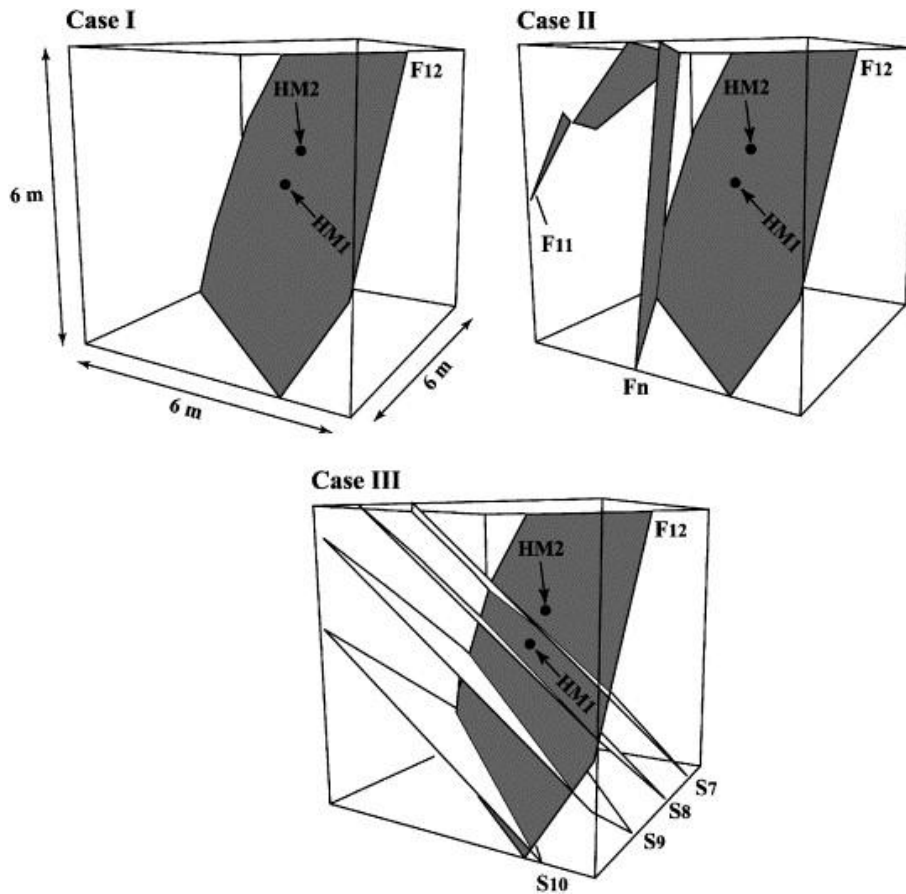


Fig. 7. Geometry of the three-dimensional numerical models: (a) axisymmetric model of a single vertical fracture (model 3DAxM); (b) 3DEC model of the fractures network around the HM1 injection point (model 3DFM); (c) boundary conditions used in 3DEC models and presented along a diagonal section aligned on HM1 and HM2 (dashed line in 7b).

- A series of two-dimensional analyses are then conducted, both with a number of axisymmetric models of the pressurized fracture embedded in an equivalent elastic media— $2DA \times M_1$ – $2DA \times M_5$  in Fig. 5a—and a model of a vertical cross section, CSM in Fig. 5b, which includes an explicit fracture network. These analyses were conducted to investigate the influence of mechanical and hydraulic boundary conditions, the unsaturated zone, fracture properties, and the leakage into the rock matrix and intersecting fractures.
- The pulse tests are then simulated within a small-scale, three-dimensional, discrete fracture model centred on the pressurized zone— $3DFM$  in Fig. 7b. A sensitivity analysis is then conducted to investigate the effects of fractures and rock matrix hydromechanical parameters and the geometry of the fracture network.

#### 4.2. Geometry, boundary conditions, and basic assumptions

Figs. 5–8 present an overview of the geometry and boundary conditions of the respective models. Models  $2DA \times M_1$ – $5$  and  $3DA \times M$  simulate axisymmetric behaviour, whereas models CSM and  $3DFM$  include near-field geometry with explicit faults and bedding planes embedded in the rock matrix. Table 1 summarises the model geometries, the numerical codes, and parameters tested in the sensitivity study.



*Fig. 8. 3D numerical models used in the sensitivity study to address the importance of the geometry of the fracture network on the hydromechanical response.*

Table 1. Codes, models, and tested parameters in each numerical approach

Numerical analysis	2D Axisymmetric	2D plane strain	3D axisymmetric	3D fractures network
Numerical simulator	ROCMAS	UDEC/ROCMAS	3DEC/ROCMAS	3DEC
Numerical model	<ul style="list-style-type: none"> <li>• 2DA×M<sub>1</sub></li> <li>• 2DA×M<sub>2</sub></li> <li>• 2DA×M<sub>3</sub></li> <li>• 2DA×M<sub>4</sub></li> </ul>	<ul style="list-style-type: none"> <li>• 2DA×M<sub>1</sub></li> <li>• 2DA×M<sub>2</sub></li> <li>• CSM</li> </ul>	<ul style="list-style-type: none"> <li>• 3DA×M</li> </ul>	<ul style="list-style-type: none"> <li>• 3DFM</li> <li>• CASE I</li> <li>• CASE II</li> <li>• CASE III</li> </ul>
Parameters tested in the sensitivity study	<ul style="list-style-type: none"> <li>• Mechanical boundary conditions</li> <li>• Radius of influence</li> <li>• Fracture normal stiffness</li> <li>• Young's modulus of rock matrix</li> <li>• Rock matrix permeability</li> <li>• Adjacent parallel fractures</li> </ul>			
	<ul style="list-style-type: none"> <li>• Mechanical boundary conditions</li> <li>• Unsaturated zone</li> <li>• Fracture hydraulic aperture</li> <li>• Fracture normal stiffness</li> <li>• Young's modulus of rock matrix</li> <li>• Geometry of the fractures network</li> </ul>			

Model 2DA×M<sub>1</sub> corresponds to a two-dimensional model (10 m × 10 m) of a single fracture surrounded by a deformable rock mass. Fig. 6 shows a close-up view of the finite element mesh assigned to ROCMAS model. The two-dimensional analysis can be executed either as a two-dimensional plane strain analysis or as an axisymmetric analysis in a vertical fracture, neglecting the gravity effects on fluid flow and mechanical deformation. All boundaries are considered to be impermeable. Initial in situ fluid pressure is 39 kPa. A constant stress is applied on the top and the right boundaries. Null vertical displacement is imposed at the bottom boundary, and, null horizontal displacement at the left boundary.

Models 2DA×M<sub>2</sub> to 2DA×M<sub>5</sub> correspond to axisymmetric models that account for different mechanical boundary conditions, different sizes, and one or two parallel fractures. These models are detailed in an axisymmetric analysis of the hydromechanical response of a fracture when subjected to a pulse test (see Section 5.2).

Model CSM is a two-dimensional model corresponding to a vertical cross section (20 m × 20 m) that represents the complete reservoir geometry through the HM1 and HM2 measuring points. All boundaries are impermeable. The topographic surface is free to move, whereas no displacement normal to the boundary is imposed at other boundaries. In situ fluid pressure is set according to the natural hydrostatic pressure gradient in the rock mass. The unsaturated zone is accounted for by affecting a zero initial saturation for fractures portions above the water table. The in situ stress gradient is set according to the weight of the overlying rock.

Model 3DA×M is a three-dimensional axisymmetric model (10 m × 10 m × 10 m) of a single fracture surrounded by a deformable rock mass. In situ and boundary conditions are the same as the ones used in the two-dimensional axisymmetric analysis (e.g., Model 2DA×M<sub>1</sub>).

Model 3DFM corresponds to a three-dimensional explicit representation of the fracture network, the case closest to reality. A discrete model of the field test around the injection zone

is analysed, taking into account three faults ( $F_{11}$ ,  $F_n$ ,  $F_{12}$ ) and four bedding planes ( $S_7$ ,  $S_8$ ,  $S_9$ ,  $S_{10}$ ) (Fig. 1 and Fig. 6). The model corresponds to a cube with 6 m sides centred at HM1. In situ and boundary fluid pressures are set according to the natural hydrostatic pressure gradient in the rock mass (Fig. 6c). On the top boundary, a vertical stress corresponding to the weight of the overburden rock mass is applied, with displacements fixed at other boundaries. The left boundary on the diagonal section of the three-dimensional model (Fig. 6c) is chosen in accordance with the sensitivity study of the mechanical boundary effects conducted in the two-dimensional analysis (see Sections 5.2 and 5.3.1). An in situ stress gradient is set according to the rock weight (Fig. 6c). Fig. 8 shows the three models (cases I–III) used in the 3DEC sensitivity study to address the importance of the geometry of the fracture network on the hydromechanical response (see Section 5.4.5).

Mechanical properties of the rock matrix and the hydromechanical properties of bedding planes (Table 2 and Table 3) are taken from previous in situ and laboratory experiments [28]. Based on previous evaluations of fault hydraulic permeability, the initial apertures of the main faults are set to  $1 \times 10^{-4}$  m.

*Table 2. Material properties used in the code comparison*

Material	Parameter	Plane strain	2D/3D axisymmetric
Fracture	Normal stiffness, $k_{nF}$ (GPa/m)	40	8
	Shear stiffness, $k_{sF}$ (GPa/m)	4	0.8
	Hydraulic aperture, $b_{hiF}$ (m)	$1 \times 10^{-4}$	$1 \times 10^{-4}$
Rock matrix	Young's modulus, $E_R$ (GPa)	70	60
	Poisson's ratio, $\nu_R$ (-)	0.29	0.29
	Mass density, $\rho_R$ (kg/m <sup>3</sup> )	2400	2400
	Permeability, $k_R$ (m <sup>2</sup> )	0	0
Fluid	Mass density, $\rho_f$ (kg/m <sup>3</sup> )	1000	1000
	Bulk modulus, $K_F$ (GPa)	2	2
	Dynamic viscosity, $\mu_F$ (Pa/s)	$1 \times 10^{-3}$	$1 \times 10^{-3}$



Table 3. Material properties used in the two-dimensional full-section analysis

Material	Parameter	UDEC	ROCMAS
Fracture	Normal stiffness, $k_{nF}$ (GPa/m)	40	40
	Shear stiffness, $k_{sF}$ (GPa/m)	4	4
	Hydraulic aperture, $b_{hiF}$ (m)	$1 \times 10^{-4}$	$1 \times 10^{-4}$
Bedding planes	Normal stiffness, $k_{nF}$ (GPa/m)	100	Not modeled <sup>a</sup>
	Shear stiffness, $k_{sF}$ (GPa/m)	10	
	Hydraulic aperture, $b_{hiF}$ (m)	$1 \times 10^{-5}$	
Rock matrix	Young's modulus, $E_R$ (GPa)	70	30 <sup>a</sup>
	Poisson's ratio, $\nu_R$ (-)	0.29	0.29
	Mass density, $\rho_R$ (kg/m <sup>3</sup> )	2400	2400
	Permeability, $k_R$ (m <sup>2</sup> )	0	0
Fluid	Mass density, $\rho_f$ (kg/m <sup>3</sup> )	1000	1000
	Bulk modulus, $K_F$ (GPa)	2	2
	Dynamic viscosity, $\mu_F$ (Pa/s)	$1 \times 10^{-3}$	$1 \times 10^{-5}$

<sup>a</sup> ROCMAS Young's modulus is reduced to implicitly account for the effects of compliant bedding planes.

In each simulation with ROCMAS, UDEC, and 3DEC, the pulse test is simulated by imposing a time-dependent pressure pulse corresponding to the pulse-pressure evolution in the injection borehole (Fig. 3a). The time-dependent borehole pressure function was programmed into each code, using standard FORTRAN for ROCMAS and special “FISH” programming [54] for UDEC and 3DEC. Thus, a consistent set of input parameters were used for each modelling approach enabling a direct comparison of between the simulation results of each model.

## 5. Modelling results and sensitivity analysis

### 5.1. Numerical capabilities and correspondence of the codes for a simple injection case

In this code comparison, model 2DA×M<sub>1</sub> is used in a two-dimensional plane strain or axisymmetric analysis, and model 3DA×M is used in a three-dimensional axisymmetric analysis. Results are almost identical for the two codes (Fig. 9). In the two-dimensional plane strain analysis, calculated curves fit perfectly the pulse-pressure-increase stage (Fig. 9a). During the pulse-pressure-decrease stage, the normal displacement magnitude is underestimated. For each code, the best fit to measurements was obtained with the same fracture and rock matrix hydromechanical properties (Table 2), including a fracture normal stiffness of 40 GPa/m. For the two- and three-dimensional axisymmetric models, the best-fit solution to field data was obtained with the two codes for a unique set of parameters (Table 2), a fracture normal stiffness of 8 GPa/m, a Young's modulus of 60 GPa, and an initial fracture aperture of  $1 \times 10^{-4}$  m. The ROCMAS solution accurately reproduces the observed behaviour with a correlation coefficient ( $r^2$ ) of 0.97 (Fig. 9b), while the 3DEC solution presents a lower correlation coefficient ( $r^2=0.57$ ) because of a discrepancy between predicted and observed

data during the pulse-pressure-decrease stage. Results are sensitive to the differences in mesh discretisation near the injection zone between the two codes. Different grid spacings produce slightly different pressure gradients near the injection point, which in turn induces a discrepancy in simulated displacement. These results show that the two modelling approaches yield almost identical results for the same set of input data, therefore providing a degree of confidence that the basic coupled hydromechanical process models are correct in each code, and that the two models for the injection experiment are correctly implemented.

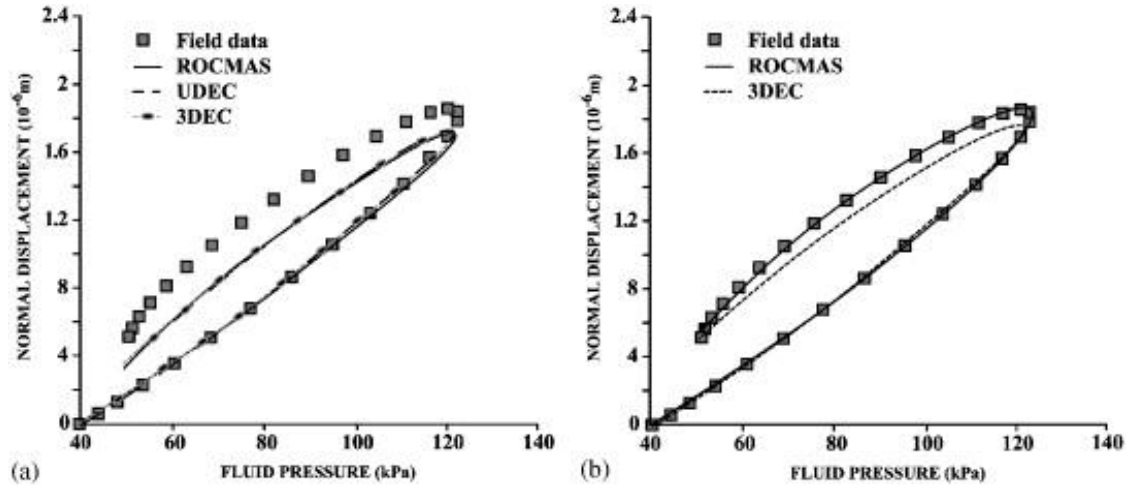


Fig. 9. Normal displacement-versus-pressure curves during pulse injection: (a) two-dimensional plane strain case simulated with ROCMAS, UDEC, and 3DEC; and (b) three-dimensional axisymmetric analysis simulated with ROCMAS and 3DEC.

## 5.2. Two-dimensional plane strain analyses

Two-dimensional plane strain analyses were conducted using the various simplified models,  $2DA \times M_1$  through  $2DA \times M_5$ , as well as the full section model, CSM, shown in Fig. 5. Fig. 10a and b present the best-match two-dimensional plane strain solutions for models  $2DA \times M_1$  and CSM, respectively. The UDEC and ROCMAS two-dimensional plane strain results are similar for the same fracture and rock-matrix hydromechanical parameters (Table 2). The key parameter for this solution is fracture normal stiffness, which is calibrated to a value of 40 GPa/m when the Young's modulus of the matrix is set to 70 GPa. The simulated loop is thinner than the one observed in the field, and a good match was obtained only for the pulse-pressure-increase stage (Fig. 10). During the pulse-pressure-decrease stage, the normal displacement magnitude is underestimated, identical to the magnitude obtained in the previous two-dimensional plane strain analysis. Fig. 10a and b show that the solution of the simple model is close to the solution of the full two-dimensional section, and there are few mechanical boundary effects as long as the boundary is at least 5 m away from the pressurized fracture. The results in Fig. 10b indicate that the use of constant stress,  $2DA \times M_1$ , or null displacement,  $2DA \times M_2$ , boundaries at the top and right side of the model does not have a significant effect on the hydromechanical response (Fig. 10a). However, it is clear from Fig. 10 that a two-dimensional plane strain analysis is incapable of reproducing the observed thickness of the displacement-versus-pressure loop. As will be explained in Section 5.3, the thickness of the loop is related to the pressure gradient near the well bore and in particular to the difference in near-well pressure gradient during the increasing and decreasing parts of the

pressure pulse. A two-dimensional plane strain analysis is not capable of representing the dominant radial-fluid-flow behaviour near the well bore and therefore the back-calculated stiffness of 40 GPa/m is not an accurate representation of the fracture normal stiffness in the field, where a dominant radial fluid flow is expected.

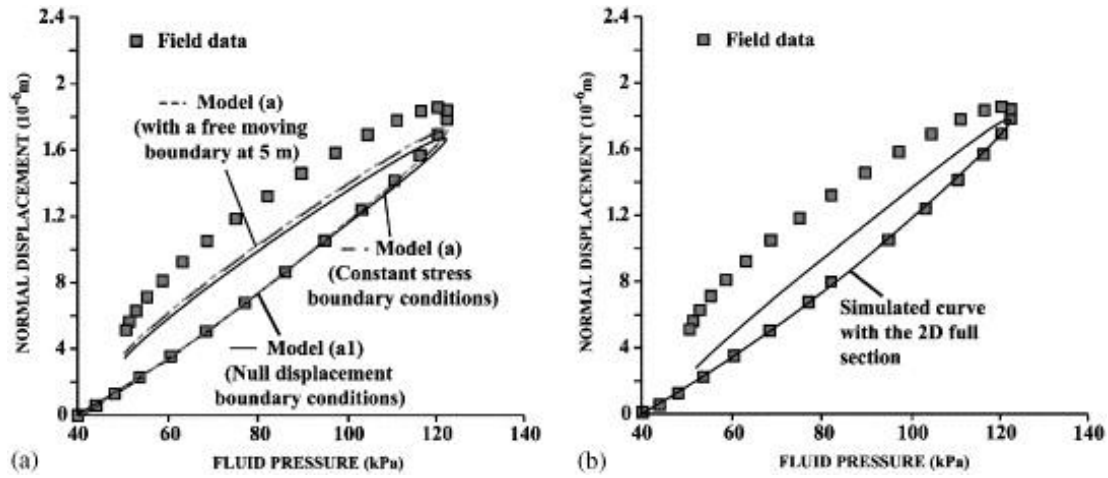


Fig. 10. Normal displacement-versus-pressure curves for (a) idealized model geometry with the pressurized fracture embedded in a homogeneous rock mass; (b) full two-dimensional model geometry.

### 5.3. Two-dimensional axisymmetric analyses

Fig. 11 presents the best-fit solution for a two-dimensional axisymmetric analysis with the ROCMAS code. Whereas the two-dimensional plane strain analysis failed to reproduce observed pressure-versus-displacement behaviour, the two-dimensional axisymmetric analysis can reproduce the observed behaviour accurately (Fig. 11). The best-fit solution in Fig. 11 was obtained with the ROCMAS code for a fracture normal stiffness of 8 GPa/m, a Young's modulus of 60 GPa, an initial fracture aperture of  $1 \times 10^{-4}$  m, and an equivalent radius of influence of about 3.9 m (Table 4). The equivalent radius of influence is a constant fluid pressure boundary representing the effect of the constant pressure conditions at water table, located at 3.9 m above the injection point. This assumption is justified by the previous analysis, which does not show the effects of the unsaturated zone on the hydromechanical response (see Section 5.2).

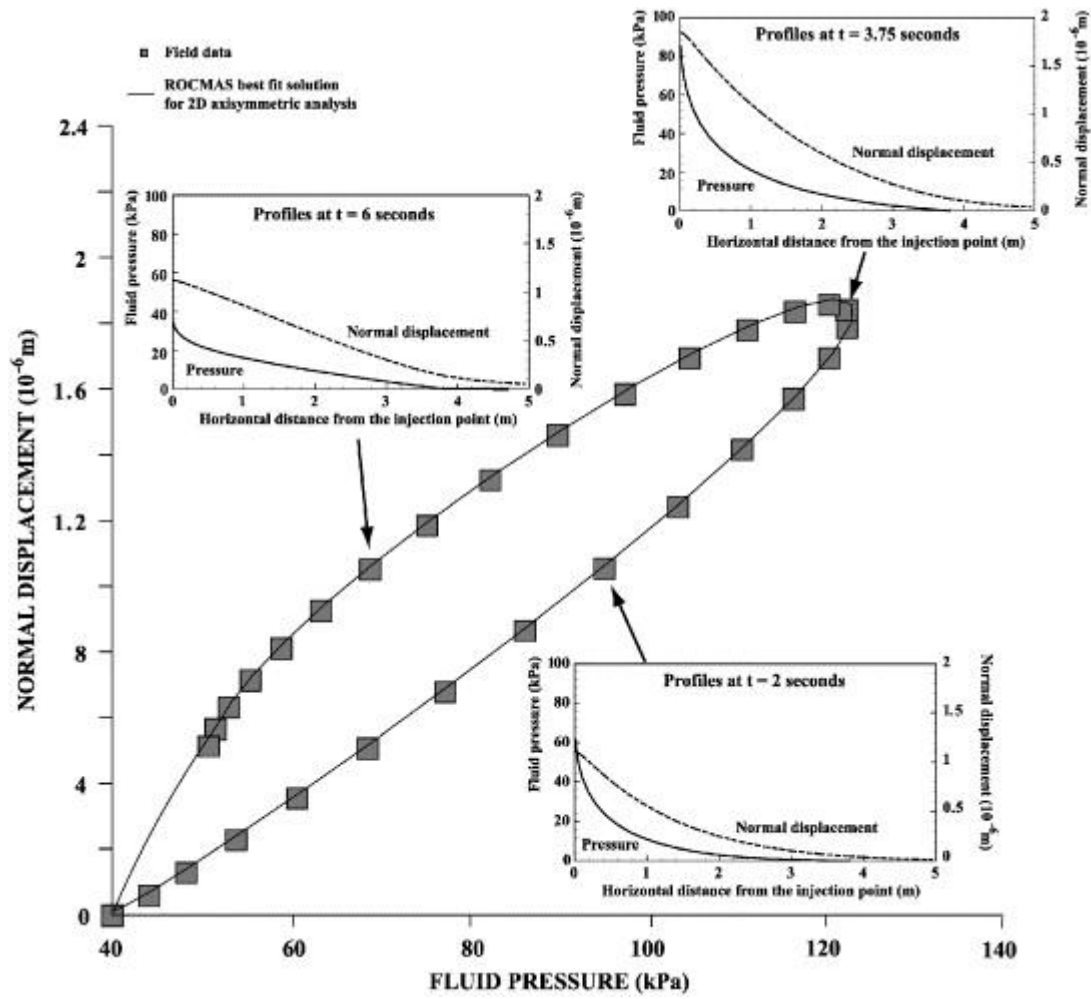


Fig. 11. The ROCMAS solution for an axisymmetric model of the pressurized fracture embedded in an elastic rock mass. The initial fracture aperture is  $1 \times 10^{-4}$  m, and the equivalent radius of influence is 3.9 m.

Table 4. Material properties used in the two-dimensional ROCMAS axisymmetric analysis and in the 3DEC full fracture network analysis

Material	Parameter	ROCMAS axisymmetric	3DEC
Faults	Normal stiffness, $k_{nF}$ (GPa/m)	8	17
	Shear stiffness, $k_{sF}$ (GPa/m)	0.8	1.7
	Hydraulic aperture, $b_{hiF}$ (m)	$1 \times 10^{-4}$	$1 \times 10^{-4}$
HM2	Normal stiffness, $k_{nF}$ (GPa/m)	Not	50
	Shear stiffness, $k_{sF}$ (GPa/m)	explicitly	5
	Hydraulic aperture, $b_{hiF}$ (m)	modeled <sup>a</sup>	$5 \times 10^{-5}$
Bedding planes	Normal stiffness, $k_{nBP}$ (GPa/m)	Not	100
	Shear stiffness, $k_{sBP}$ (GPa/m)	explicitly	10
	Hydraulic aperture, $b_{hiBP}$ (m)	modeled <sup>b</sup>	$1 \times 10^{-5}$
Rock matrix	Young's modulus, $E_R$ (GPa)	60	70
	Poisson's ratio, $\nu_R$ (-)	0.29	0.29
	Mass density, $\rho_R$ (kg/m <sup>3</sup> )	2400	2400
	Permeability, $k_R$ (m <sup>2</sup> )	0	0
Fluid	Mass density, $\rho_f$ (kg/m <sup>3</sup> )	1000	1000
	Bulk modulus, $K_F$ (GPa)	2	2
	Dynamic viscosity, $\mu_F$ (Pa/s)	$1 \times 10^{-3}$	$1 \times 10^{-3}$

<sup>a</sup> In ROCMAS axisymmetric model, HM2 is not explicitly modeled as a point.

<sup>b</sup> ROCMAS Young's modulus should implicitly account for the effects of compliant bedding planes.

Fig. 11 also presents radial profiles of fluid pressure and fracture aperture, which help to explain the cause of the measured and simulated loop behaviour. In essence, the loop behaviour occurs because the fracture hydromechanical response is a result of the distribution of fluid pressure within the fracture rather than the local fluid pressure at the well. Fracture opening measured at the well is not directly proportional to the well pressure, but is rather a result of the total force applied on the fracture surfaces from the distributed fluid pressure over a larger fracture surface area. During the pressure increasing part, the pressure gradient is very steep near the well, and as a result the fluid pressure is much higher at the well than a few metres into the fracture. During the pressure decreasing part, on the other hand, the pressure gradient is much smaller, and hence, the fluid pressure at the well is not as much higher than the fluid pressure a few metres into the fracture. For example, at 2 s, the well pressure is 60 kPa, whereas the fluid pressure at 1 m radius is about 12 kPa. At 6 s, the well pressure has decreased to about 35 kPa, whereas the fluid pressure at 1 m radius is still as high as 15 kPa. At 6 s, the fracture opening at the well bore is slightly larger than at 2 s, reflecting the fracture opening being proportional to the fluid pressure at 1 m radius rather than being proportional to the well pressure. Thus, hydromechanical behaviour of the fracture during the pulse injection test can only be reproduced by a model that correctly accounts for the dominating radial flow behaviour, including a steep pressure gradient near the well bore. This

can be done with three-dimensional or two-dimensional axisymmetric models, but not with a two-dimensional plane strain model. The fact that the measured thickness of the loop can be reproduced by an axisymmetric model, but not by a two-dimensional plane strain model, confirms that the fluid flow in the field test is indeed approximately radial near the well.

### **5.3.1. Mechanical boundary effects**

The influence of mechanical boundary conditions was examined with a suite of differently sized two-dimensional models (Fig. 5a). The effect of the outer boundaries becomes significant if they are closer than about 5 m from the pressurized fracture. A smaller sized (5 m by 7 m) axisymmetric model was used to test the influence of mechanical boundary conditions closer to the pressurized fracture (models 2DA×M<sub>3</sub> and 2DA×M<sub>4</sub>, Fig. 5a). Results show that when the boundary moves as close as 5 m from the pressurized fracture, mechanical boundary conditions have a significant influence, particularly on the thickness of the displacement-pressure curve (Fig. 12a). The full loop is thicker when the upper boundary is free to move under a constant stress. A separate analysis showed that the impact of the mechanical boundary conditions at the lateral boundary is less significant.

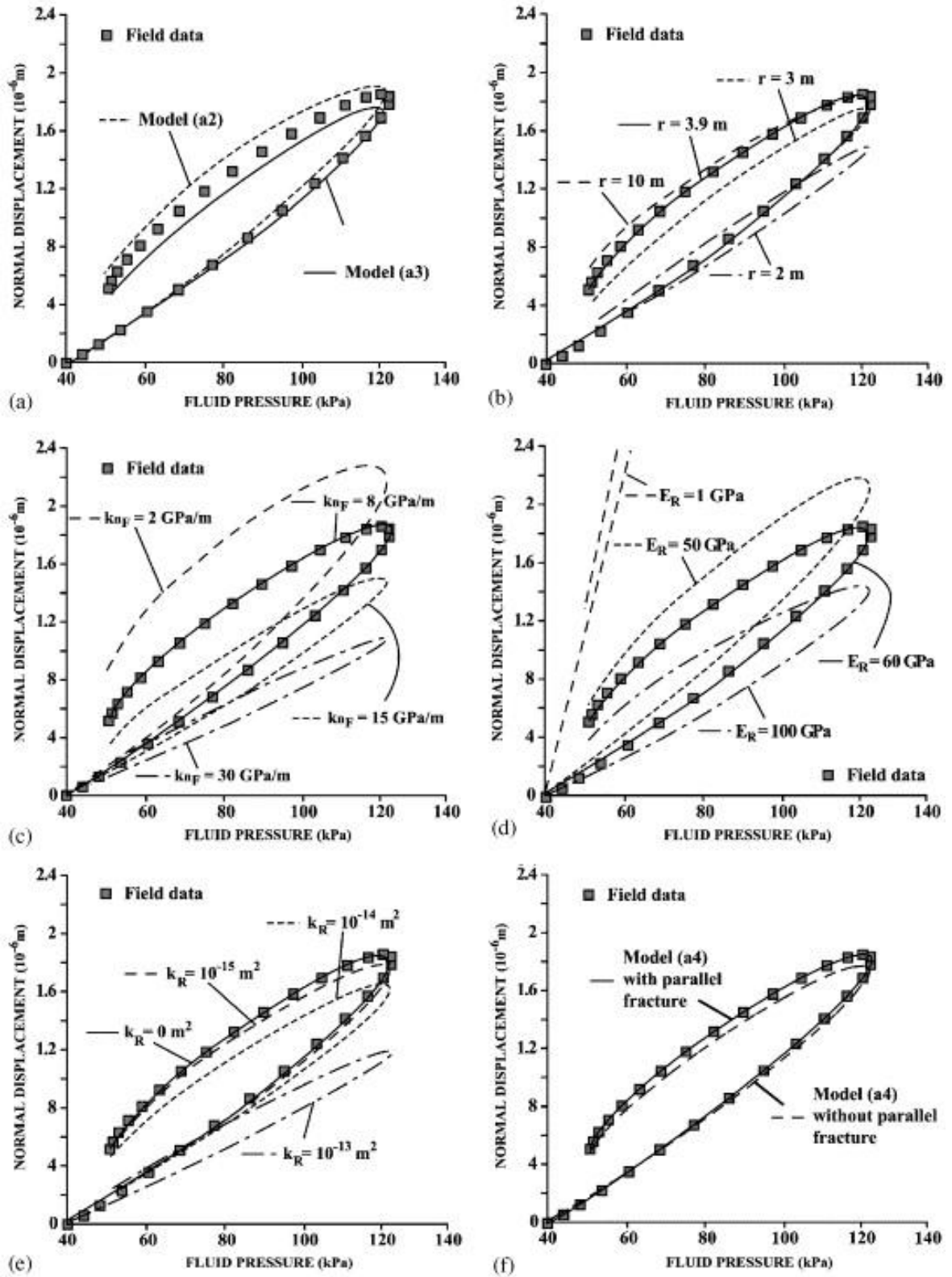


Fig. 12. Pulse test analysed using ROCMAS at  $HM_1$  while varying (a) mechanical boundaries; (b) equivalent radius of influence; (c) fracture normal stiffness; (d) Young's modulus of rock matrix; (e) leakage into rock matrix; (f) adjacent fault.

### 5.3.2. Effects of radius of influence

In the field, the water table is located about 3.9 m above  $HM_1$ , and other fractures intersect the pressurized fault at various distances from  $HM_1$ . The composite effect of the water table and water leakage into intersecting fractures may work as a constant fluid pressure boundary. In the axisymmetric model, these conditions may be represented by a constant pressure boundary at a certain equivalent radius from the injection point. In this case, the effect of the equivalent fluid pressure radius is tested using the model  $2DA \times M_1$ , by imposing a constant fluid pressure at various radii along the fracture. Fig. 12b shows that the effect of such a constant pressure boundary would become significant if it were located closer than about 3.9 m from the injection point. A short radius of influence tends to prevent fracture opening because the pressurized area is smaller, and thereby the total force exerted on the fracture walls is smaller. If the radius is larger than 3 m, the effect is only visible for the pulse-pressure decreasing stage.

### 5.3.3. Effects of fracture normal stiffness and elastic effects of rock matrix

The sensitivity to fracture normal stiffness and Young's modulus of the rock matrix is analysed with model  $2DA \times M_1$ . The effect of fracture normal stiffness is studied for a fixed Young's modulus of 60 GPa (Fig. 12c), while the effect of the Young's modulus for the rock matrix is analysed for a fixed fracture normal stiffness of 8 GPa/m (Fig. 12d). Results show that fracture hydromechanical response is sensitive to changes in both fracture normal stiffness and the Young's modulus of the surrounding rock mass. Moreover, results show that if the fracture normal stiffness is much larger than the stiffness of the rock matrix, no loop appears in the displacement-versus-pressure curve. When the Young's modulus increases, a loop-shaped evolution appears. The loop appears because of dominant radial-fluid-flow behaviour near  $HM_1$ , which provides a steep pressure gradient combined with a fracture opening restricted by the stiffness of the surrounding rock.

### 5.3.4. Leakage into matrix and poro-elastic effects

The effect of leakage from the main fracture into the surrounding rock matrix was analysed with model  $2DA \times M_1$ . Results show that there is no significant leakage into the rock matrix unless the matrix permeability is higher than  $1 \times 10^{-15} \text{ m}^2$  (Fig. 12e). For a very high matrix permeability of  $1 \times 10^{-14}$  or  $1 \times 10^{-13} \text{ m}^2$ , the fracture opening is smaller, because leakage into the rock matrix reduces the amount of fluid flowing, and thus the pressure increase along the fracture. A shorter penetration distance implies that the total force of the fluid pressure applied on the fracture wall would be smaller. However, the matrix permeability is not likely to be higher than  $1 \times 10^{-15} \text{ m}^2$ , even if the permeability of the bedding planes and small-scale discontinuities were included. Thus, leakage into the rock matrix and associated poro-elastic effects do not appear to be significant for the fracture hydromechanical responses during these pulse injection tests.



### 5.3.5. Effects of adjacent faults

The manner in which adjacent faults affect the hydromechanical response of the pressurized fracture is simulated with an axisymmetric model that includes two parallel horizontal fractures (model 2DA×M<sub>5</sub>, Fig. 5a). Model dimension is 7 m × 5 m and roughly corresponds to the distance from HM1 to the free surface of the rock slope. Fracture spacing is 2.5 m and corresponds to the distance between F<sub>11</sub> and F<sub>12</sub> in the full section (Fig. 5b). For model 2DA×M<sub>5</sub>, the best-match parameters of Young's modulus and fracture normal stiffness are slightly different than for model 2DA×M<sub>1</sub>. The full loop-shaped evolution is reproduced when the model includes the two fractures with a fracture normal stiffness  $k_n=12$  GPa/m and Young's modulus  $E=60$  GPa (Fig. 12f). When including the adjacent faults, the rock mass becomes more compliant (compared to the otherwise intact rock) and as a result, an additional fracture opening is induced, especially during the fluid-pressure decrease stage. This additional deformation occurs because the adjacent fracture is compressed and thereby accommodates deformation in the pressurized fracture deformation.

## 5.4. Three-dimensional analysis: distinct element modelling and sensitivity analysis

In this section, we present results of the sensitivity study conducted with 3DEC to address the importance of the fractures and rock matrix properties, as well as the 3D geometry of the fracture network to the hydromechanical response of the rock mass.

### 5.4.1. Reference case

The best match was obtained for different values of initial normal stiffness and initial hydraulic aperture at HM1 and HM2 within fault F<sub>12</sub> (Table 4 and Fig. 13a). In the best match solution, fracture properties are assumed to be equal to that of HM1 in the entire fracture except to a radius of about 30 cm from HM2, where the HM2 properties are assumed. The initial-normal-stiffness value at HM2 is a factor of 3 higher than at HM1, and the initial-hydraulic-aperture value at HM2 is a factor of 2 lower than at HM1. These best-fitting parameters were verified both at HM1 and HM2, while simulating two other pulse tests. A pulse of 9 kPa at HM1 and a pulse of 35 kPa at HM2 were simulated. The two simulations (Fig. 13b and 13c) yielded best match to field data for fracture hydromechanical parameters similar to the ones previously obtained with the pulse of 86 kPa (Table 4). This model verification confirms that the hydromechanical properties varied by a factor of 2–3 for the two points spaced 1 m apart along the same fracture plane.

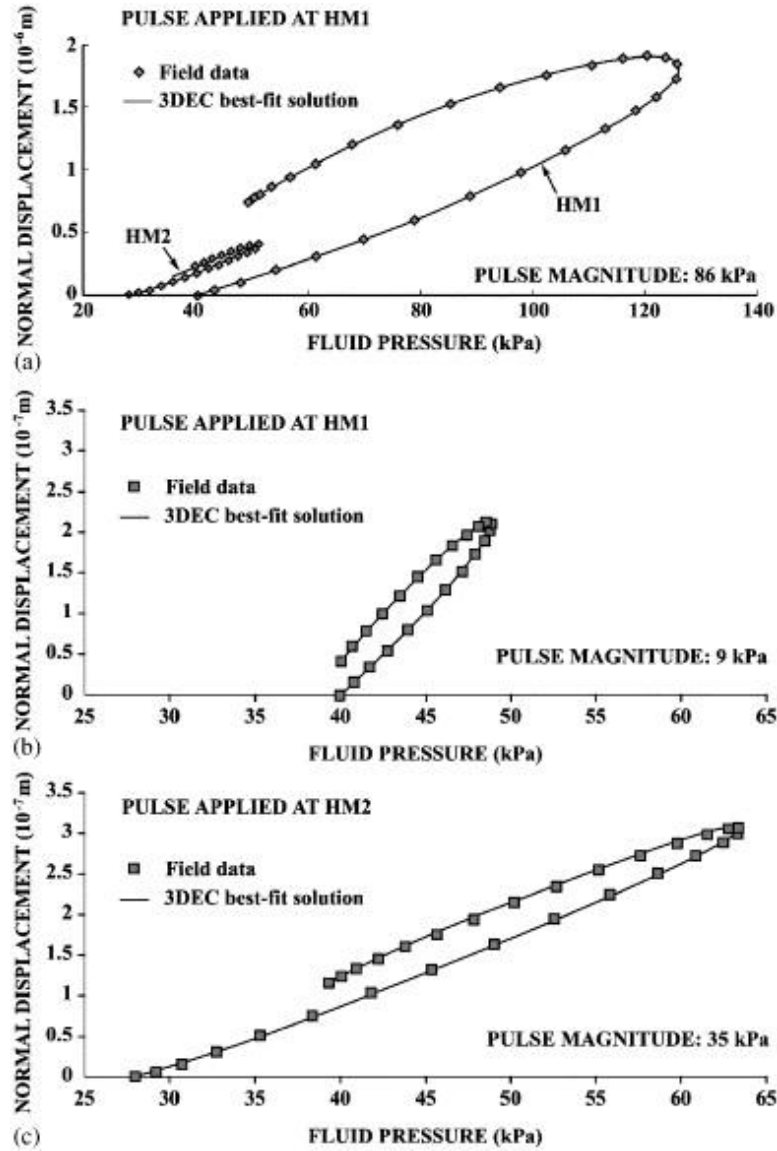
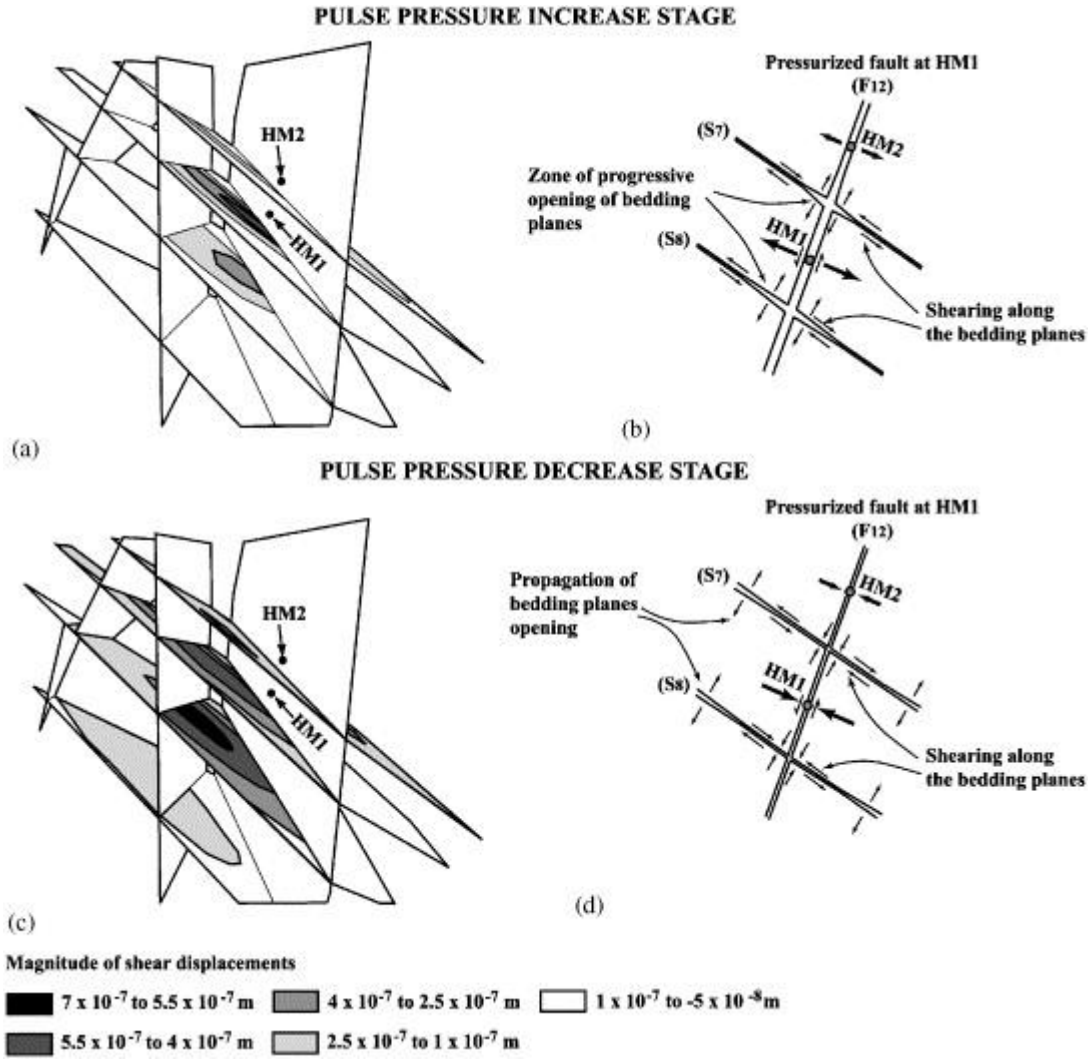


Fig. 13. Pulse test analysed using 3DEC with best-fit parameters for (a) reference case; (b) HM1 parameters from the reference case used for a pulse magnitude of 9 kPa at HM1; (c) HM2 parameters from the reference case used for a pulse magnitude of 35 kPa at HM2.

The best-fit solution (Fig. 13a) indicates that the pressure gradient caused by the injection is mainly distributed in the pressurized fault. Nevertheless, part of the fluid flow occurs within the bedding plane ( $S_7$  and  $S_8$ ) close to HM1 and HM2. During the pulse-pressure increase, fluid flow within the bedding planes induces a progressive mechanical opening along these planes (Fig. 14). In the vicinity of the pressurized fault, the normal displacement of bedding planes reaches a significant value by higher magnitude ( $\Delta U_n = 6 \times 10^{-7}$  m) than the one for the fault at HM2 (Fig. 14). In addition, fault opening causes shearing along bedding planes whose magnitude ( $\Delta U_s = 4.25 \times 10^{-7}$  m) is of the same order as the normal displacement at HM2. During pressure-pulse decrease at the injection point, progressive bedding plane opening still occurs far from the pressurized fault, whereas near the fault, bedding planes tend to close (Fig. 14). This is caused by the propagation of the pressure within the bedding planes, in which pressure increases away from the injection point with a certain time delay. This induces delayed mechanical displacement.



*Fig. 14. (a and b) Simulated shear displacement along bedding planes during the pressure pulse at HM1; (c and d) 2D conceptual model of deformation along bedding planes during the pressure pulse at HM1.*

In summary, this best-fit case shows a slight difference in fracture hydromechanical properties for two nearby points within the same fault plane. During a pulse injection test, hydromechanically and mechanically induced fracture shear and normal deformations occur in bedding planes and faults within the rock mass surrounding the tested fracture. These effects contribute significantly to accommodate the deformation of the pressurized fracture, and can therefore explain the difference between the back-calculated fracture normal stiffness with the three-dimensional single fracture axisymmetric model ( $k_{nF}=8$  GPa/m) and the stiffness obtained using the three-dimensional fracture-network model ( $k_{nF}=17$  GPa/m). This best-fit solution is used as a reference case in a sensitivity study presented below to address the importance of each hydromechanical parameter, as well as the importance of the fracture-network geometry with respect to the hydromechanical response of the pressurized fault  $F_{12}$  in HM1 and HM2. The properties of faults  $F_{11}$  and  $F_n$  are assumed to be identical to those at HM1.

### 5.4.2. Effects of tested fault hydraulic aperture and normal stiffness

The initial normal stiffness of the fault strongly affects the normal displacement-versus-fluid pressure response at HM1 (Fig. 15a). If the fault normal stiffness is lower than the reference value ( $k_{nF} = 17$  GPa/m), the normal displacement and the thickness of the loop increase. Conversely, if the normal stiffness value is higher, the normal displacement and the loop thickness decrease. The results for variation of fracture normal stiffness, shown in Fig. 15a, is consistent with the results obtained in the previous axisymmetric analysis shown in Fig. 12c. At HM2, the hydromechanical response follows the same variation as at HM1, but the magnitude of displacement is lower, because of the difference in initial normal stiffness between the two points (Fig. 16a).

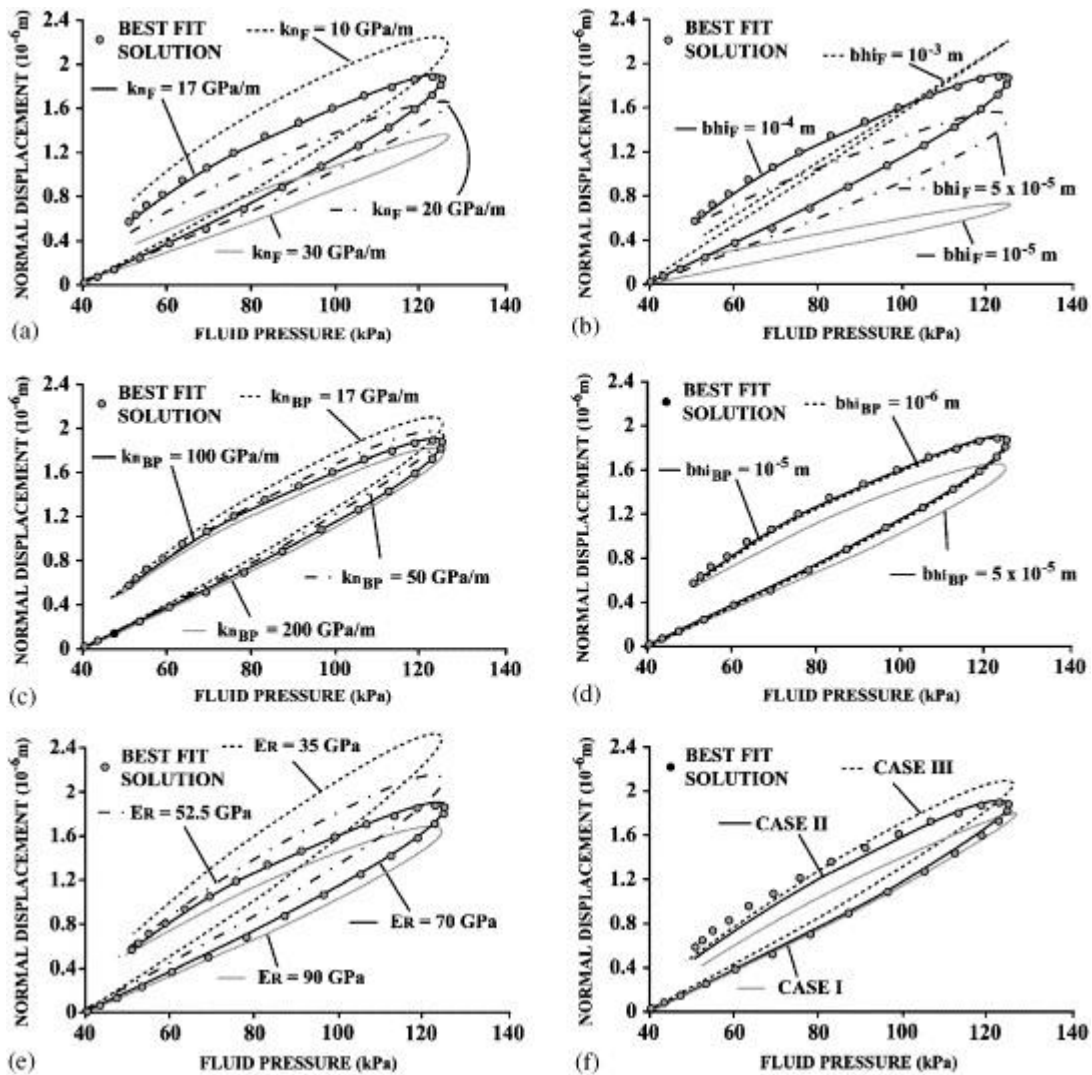


Fig. 15. 3DEC analysis of a pulse test applied to HM1 while varying (a) initial normal stiffness at HM1 ( $k_{nF}$ ); (b) initial hydraulic aperture at HM1 ( $B_{hiF}$ ); (c) initial normal stiffness of bedding planes ( $k_{nBP}$ ); (d) initial hydraulic aperture of bedding planes ( $B_{hiBP}$ ); (e) Young's modulus of rock matrix ( $ER$ ); (f) geometry of the fracture network (Cases I–III).

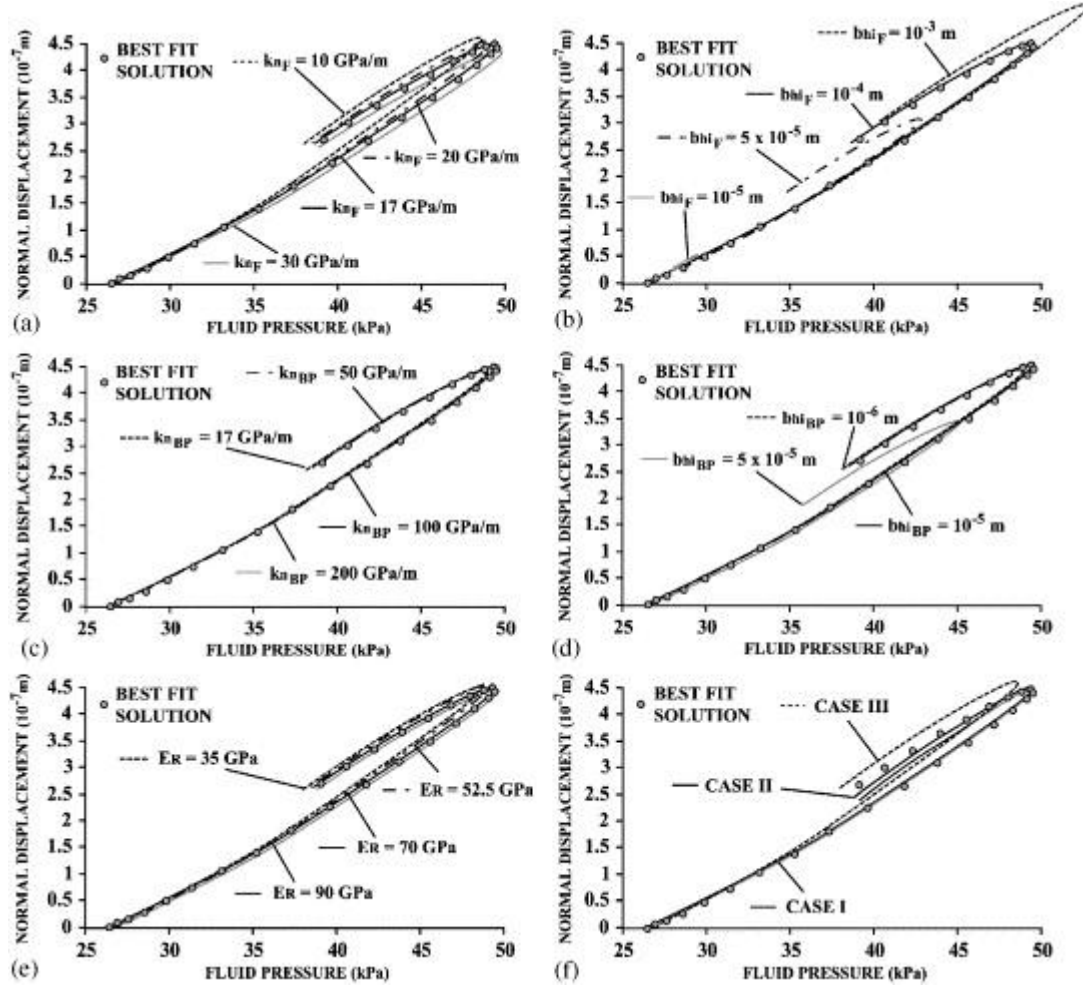


Fig. 16. 3DEC sensitivity analysis at HM2 for a pulse test applied to HM1 while varying  
(a) initial normal stiffness at HM1 ( $k_{nF}$ ); (b) initial hydraulic aperture at HM1 ( $b_{hiF}$ );  
(c) initial normal stiffness of bedding planes ( $k_{nBP}$ ); (d) initial hydraulic aperture of bedding  
planes ( $b_{hiBP}$ ); (e) Young's modulus of rock matrix ( $ER$ );  
(f) geometry of the fractures network (Cases I–III).

At HM1, a variation in the fault initial hydraulic aperture by a factor of 10 has a significant influence on both the magnitude of normal displacement and the loop thickness (Fig. 15b). The larger the initial hydraulic aperture, the higher the magnitude of normal displacement. For a larger initial aperture, fluid pressure penetrates farther into the fracture for a given well pressure and time increment. The fact that the fluid pressure penetrates farther for a larger initial aperture can be observed as a higher magnitude of fluid pressure at HM2. The more widespread the pressure increase (with a larger radius of influence), the greater the force on the fracture surfaces, which in turn induces additional fracture opening. Moreover, if the initial hydraulic aperture of the pressurized fault is large compared to intersecting bedding-plane apertures, fluid flow and pressure-deformation predominantly takes place within the fault.

If, on the other hand, the initial hydraulic aperture of the pressurized fault is equal or close to bedding-plane apertures (aperture of all bedding planes is  $1 \times 10^{-5}$  m for the results in Fig.

15b), a significant amount of water leaks into intersecting bedding planes. This means that fluid pressure will not penetrate as far along the fault, but will rather leak into intersecting bedding planes, and then the pressure-deformation coupling occurs in the entire network. Modelling results indicate that the permeability contrast between the pressurized fault and bedding planes also affects the pressure-displacement responses in HM2 (Fig. 16b). Note Fig. 16b shows that the hydraulic aperture of the pressurized fracture strongly affects the magnitude of fluid pressure seen in HM2, whereas other parameters have negligible effect. Thus, the aperture value of  $1 \times 10^{-4}$  m, which was obtained from previous field experiments of the site, is verified by the good match in Fig. 16b.

#### **5.4.3. Hydraulic apertures and normal stiffnesses of four bedding planes**

Variation of bedding-plane normal stiffness from 50 to 200 GPa/m has no significant effect on the hydromechanical response at HM1 or HM2 (Fig. 15 and Fig. 16). A bedding-plane stiffness value equal to that at the fault has a minor effect at HM1.

A decrease in bedding-plane initial hydraulic aperture by a factor of 10 has no effect on the displacement-pressure response (Fig. 15 and Fig. 16). A hydraulic-aperture increase by a factor of 5 causes a slight decrease of normal displacement at HM1, and a slight decrease of normal displacement and fluid pressure at HM2. This is also caused by the fact that the fluid pressure effect on fault deformation depends on the hydraulic aperture contrast between the main fault and the bedding planes. As discussed in Section 5.4.1, if the bedding-plane apertures are equal or close to the initial hydraulic aperture of the pressurized fault, a significant amount of the injected water leaks into intersecting bedding planes. Since leakage into bedding planes reduces the amount of fluid flowing along the fracture, it prevents fluid pressure from penetrating farther in the fracture. A smaller radius of influence results in a smaller fracture opening, because the pressurized fracture area is smaller, and so the total force exerted on the fracture walls is smaller. This behaviour is qualitatively the same as that obtained for leakage into a highly permeable rock matrix in the previous ROCMAS simulations (see Fig. 12e).

#### **5.4.4. Young's modulus of rock matrix**

A decrease in the Young's modulus by a factor of 1.5 or 2 significantly increases the normal displacement (by several microns) at HM1 (Fig. 15e). An increase of the Young's modulus by a factor of 1.5 slightly decreases the normal displacement at HM1. Again, the results are in qualitative agreement with the results obtained in the previous axisymmetric ROCMAS analysis (Fig. 12d). The magnitudes of pressure changes, on the other hand, are not affected. At HM2, varying the Young's modulus induces a small change in displacement magnitude (Fig. 16e), because the higher stiffness at HM2 restricts the effect of rock matrix deformation on fracture deformation.

#### **5.4.5. Geometry of the fractures network**

The effects of fracture network geometry on the hydromechanical response of the pressurized fault ( $F_{12}$ ) were highlighted by comparing the following three numerical cases (Fig. 8):

- Fault  $F_{12}$  embedded in the intact rock matrix (Case I)
- Faults  $F_{11}$ ,  $F_n$ ,  $F_{12}$ , and no bedding planes (Case II)
- Bedding planes  $S_7$ ,  $S_8$ ,  $S_9$ ,  $S_{10}$  and fault  $F_{12}$  (Case III).

Results presented for points HM1 (Fig. 15f) and HM2 (Fig. 16f) show that Case I, with the pressurized fault,  $F_{12}$ , embedded in intact rock matrix, matches the experimental values only for the pulse-pressure increasing part. Thus, under the initial pressure increase, the near-well-bore intrinsic hydromechanical properties of fault  $F_{12}$  and the mechanical properties of the surrounding rock matrix control the fault behaviour. For the subsequent pressure-decreasing part, on the other hand, Case I underestimates the fracture opening because the opening is restricted by the relatively stiff rock matrix. Case II, which includes faults and intact rock matrix, but neglects the bedding planes, matches the field data for the pulse-pressure increase and half of the pulse-pressure decrease. Qualitatively, the different pressure-versus-displacement behaviour for Cases I and II is consistent with the results in Fig. 12f for adjacent parallel fractures. This result confirms that the adjacent faults ( $F_{11}$  and  $F_n$ ) affect the hydromechanical response of the pressurized fault during the pulse-pressure decrease stage. Case III, which includes the four bedding planes and the fault  $F_{12}$ , but neglects the parallel faults ( $F_{11}$  and  $F_n$ ), does not match the measurements. Normal displacement is overestimated because the additional shearing that occurs along bedding planes causes an amplification of the fault mechanical aperture.

## 5.5. Comparison of 3DEC and ROCMAS best-fit model parameters

The 3DEC and ROCMAS mechanical parameters back-calculated by fitting field data (Table 4) are consistent in the sense that ROCMAS represents all discontinuities surrounding the pressurized fracture as an equivalent continua. The fracture and rock matrix stiffness values back calculated in ROCMAS are thus lower than the ones obtained with 3DEC. The difference in back-calculated fracture normal stiffness (8 GPa/m for ROCMAS and 17 GPa/m for 3DEC) can be directly linked to the contribution from the deformability of fractures in the surrounding rock mass, and in particular to the shear deformations in intersecting bedding-planes. In the 3DEC analysis, shear displacements along bedding-planes accommodate additional deformation at HM1, as described in Fig. 14c. In the 3DEC analysis, a higher fracture normal stiffness of the main fracture offsets the effect of this bedding-plane shear-displacement. However, the ROCMAS best-fitting mechanical parameters are equivalent parameters that allow the restoration of mechanical deformations caused by adjacent fractures. Thus, with both a discrete or continuum model, a reasonable range of values is obtained, provided that the effective parameters account for mechanical deformations being accommodated by the surrounding discontinuities.

## 6. Discussion

### 6.1. Implications for in situ determination of hydromechanical properties

The two-dimensional plane strain and axisymmetric analyses, and the three-dimensional fracture-network analysis, identify the key parameters that influence field-test results (Table 5). The analyses indicate that the two most important parameters determining the normal displacement-versus-pressure responses are the fracture normal stiffness and the Young's modulus of the intact rock. In addition, the initial aperture of the fault is an important parameter that (in this case) had been determined from previous hydraulic tests at the site. Both the fracture normal stiffness and the stiffness of the surrounding rock mass (represented by the Young's modulus) affect the general form of the normal displacement-versus-pressure curve. However, the width of the loop-shaped evolution of the normal displacement-versus-pressure curve is mainly a function of the contrast between the fracture and rock-matrix

mechanical properties. Because the loop shape is controlled by this stiffness contrast, it might be possible to uniquely determine both fracture normal stiffness and an equivalent modulus of the surrounding rock mass by examining both the inclination and width of the loop. However, such an analysis would require accurate modelling of the full loop of both increasing pressure and decreasing pressure in an ideal radial-symmetric-flow situation. The sensitivity analysis indicates that if one or several highly permeable fractures intersect the pressurized fracture close to the injection point, leakage may induce significant deviations from this ideal solution and lead to significant error in the back-calculated stiffness values. Furthermore, shear slip along bedding planes intersecting the pressurized fault can result in additional fracture opening near the well bore, which would impact the back-calculated hydromechanical properties of the pressurized fault.

*Table 5. Importance of each parameter for the sensitivity of the hydromechanical response*

Numerical analysis	Tested parameters	Influence on the hydromechanical response
2D Axisymmetric	<ul style="list-style-type: none"> <li>• Mechanical boundary conditions</li> <li>• Radius of influence (r)</li> <li>• Fracture initial normal stiffness (knF)</li> <li>• Young's modulus of rock matrix (ER)</li> <li>• Rock matrix permeability (kR)</li> <li>• Adjacent parallel fault</li> </ul>	<ul style="list-style-type: none"> <li>• No effects<sup>a</sup></li> <li>• No effects<sup>b</sup></li> <li>• High</li> <li>• High</li> <li>• No effects<sup>c</sup></li> <li>• High</li> </ul>
2D plane strain	<ul style="list-style-type: none"> <li>• Topographic surface</li> <li>• Unsaturated zone</li> </ul>	<ul style="list-style-type: none"> <li>• No effects</li> <li>• No effects</li> </ul>
3D fractures Network	<ul style="list-style-type: none"> <li>• Fracture initial hydraulic aperture (b<sub>hiF</sub>)</li> <li>• Fracture initial normal stiffness (k<sub>nF</sub>)</li> <li>• Bedding planes initial hydraulic aperture (b<sub>hiBP</sub>)</li> <li>• Bedding planes initial normal stiffness (k<sub>nBP</sub>)</li> <li>• Young's modulus of rock matrix (ER)</li> <li>• Geometry of the fractures network</li> </ul>	<ul style="list-style-type: none"> <li>• High</li> <li>• High</li> <li>• No effects</li> <li>• No effects</li> <li>• High</li> <li>• High</li> </ul>

<sup>a</sup> if the distance between the boundary parallel to the pressurized fracture and HM1 is equal or higher than 5 m

<sup>b</sup> if 'r' is equal or higher than 3.9 m

<sup>c</sup> if 'k<sub>R</sub>' is higher than 10<sup>-15</sup> m<sup>2</sup>

## 6.2. Heterogeneity of fracture hydromechanical properties

The current study demonstrates that fracture hydromechanical properties, such as hydraulic aperture and normal stiffness, can vary by a factor of 2–3 along the same plane. This difference could be explained by the complex internal geometry of voids and by the distribution of the amount and size of the contact surface area within the fracture plane. Such variability of fracture hydromechanical properties, with pressure distribution and fracture normal stiffness implicitly linked to the geometry of void space and contact area, has also been observed for single joints at the laboratory scale [14] and [15]. For example, Hopkins et al. [61] show that joint normal stiffness in laboratory samples can vary over several orders of magnitude, along with the percentage of contact area within the joint. On a larger scale,



measurements of fault aperture in the field have shown that a shear offset results in an undulating pattern of variable fracture aperture [44] and [62]. Faults would then be better represented with a variable width of void space and a variable contact area, to take into consideration the variability of hydraulic aperture and normal stiffness [17], [18], [23] and [63]. Moreover, this geometry could also allow consideration of channelling within the fault plane. Channelling can induce a so-called insufficient effect of pressure rise, in which pressure rise can induce variable displacement along the fault plane, like those observed at HM1 and HM2. This effect has been experimentally observed at the laboratory scale and physically linked to the discontinuity morphology [64] and [65]. Therefore, if at all possible, hydraulic tests such as interference tests should be performed at different points to characterize the fracture inhomogeneous hydraulic and mechanical properties. Thus, one measuring point may not be sufficient as a representative value over the fracture plane.

### **6.3. Contribution of the hydromechanical analysis to accurately estimate fracture hydraulic aperture**

Commonly, the determination of hydraulic aperture of a fracture using pulse test data is made by curve-matching techniques [45], [46], [47], [48], [49] and [50]. In these approaches, fracture permeability is determined by matching the pulse-pressure-decreasing stage to type curves, with the effects of hydromechanical processes lumped into an “effective” fracture storativity value. Fracture hydraulic aperture is then derived from permeability, using the cubic law [60]. Compared to these techniques, our modelling approach analyses all the pressure curves and indicates that the pressure decrease path of a pulse test is highly influenced by mechanical processes within a larger portion of the surrounding fractured rock, whereas the pressure increase path of the normal displacement-versus-pressure curve allows us to constrain the determination process of the tested-fracture's hydromechanical properties. Thus, the pressure-decrease path used in the analytical techniques is not representative of the behaviour of the tested fracture and therefore provides equivalent properties of the surrounding fractured rock. To avoid such misinterpretations, the analytical solutions should be adapted to include an analysis of the pulse-pressure-increase stage, which provides an accurate estimate of the hydraulic aperture of the tested fracture. Additionally, in the evaluation process, displacement-versus-pressure measurements can be used to constrain fracture storativity. Indeed, Rutqvist et al. [37] show that in the fracture, the water storage is dominated by the fracture deformation. Thus, with coupled pressure and displacement measurements across the fracture, we could obtain a reasonable range of intrinsic hydraulic properties for the tested fracture. The high-frequency simultaneous measurements of the fluid pressure and mechanical displacement changes are useful in monitoring the complete transient hydromechanical reactions that a fracture might undergo during a pulse injection test, and in providing a substantially improved estimate of fracture hydraulic aperture. The complete pressure–time and normal displacement–time curves, including increasing and decreasing stages, are necessary for an accurate analysis of this type of pulse test.

### **6.4. Mechanical behaviour of the rock mass surrounding the pressurized fracture**

The loop-shaped normal displacement-versus-pressure curve clearly gives two distinct kinds of information about the tested fracture zone. Modelling shows that a hydromechanical analysis of the pulse-pressure-increase stage can provide an estimate of fracture hydromechanical properties, provided that the Young's modulus of the rock matrix is known.

Moreover, the pulse-pressure-decrease stage is affected by the hydraulic and mechanical properties of the surrounding fractured rock mass. Comparison between the three-dimensional best-fit model and the simpler models shows that aperture closure and shearing would occur in discontinuities surrounding the pressurized fault  $F_{12}$ —and these may have a significant impact on the hydromechanical response of the tested discontinuity. This means that the pressure-decrease stage of a pulse test could be used to characterize the equivalent mechanical properties of the rock mass surrounding the tested discontinuity, which includes the composite effect of fracture network geometry and the porous rock matrix.

## **7. Conclusions**

Pulse testing with high-frequency measurements of fluid pressure and mechanical displacement has been shown to be a useful new method for in situ characterization of fractured-rock masses. The method can be used to determine hydraulic aperture and normal stiffness of fractures, as well as the equivalent stiffness of the surrounding rock mass. However, evaluation of such a pulse injection test by hydromechanical modelling should be performed with great care, because the fracture system response is complex. The tests can be evaluated by models considering either a discrete fracture medium, or an idealized medium made up of a pressurized fracture embedded in an equivalent rock medium that reproduces the behaviour of the surrounding fractured rock. Potential misinterpretations of fracture hydraulic aperture by conventional curve matching of pulse-pressure data can be avoided with an appropriate hydromechanical analysis of the complete pressure–time and displacement–time curves, including results from both the pressure-increasing and pressure-decreasing stages.

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