



Fracture growth leading to mechanical spalling around deposition boreholes of an underground nuclear waste repository

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ABSTRACT

This study presents a three-dimensional numerical analysis of multiple fracture growth leading to spalling around nuclear waste deposition boreholes. Mechanical spalling due to stress amplification after drilling is simulated using a finite element-based fracture growth simulator. Fractures initiate in tension based on a damage criterion and grow by evaluating stress intensity factors at each fracture tip. Tip propagation is multi-modal, resulting in final fracture patterns that are representative of both tensile and shear failure. Their geometries are represented by smooth parametric surfaces, which evolve during growth using lofting. The corresponding surface and volumetric meshes are updated at every growth step to accommodate the evolving fracture geometries. The numerical model is validated by comparing simulated fracture patterns against those observed in the AECL Underground Rock Laboratory Mine-By Experiment. It is subsequently calibrated to simulate fracture initiation and growth around boreholes drilled in the Forsmark granodiorite, subjected to a far-field anisotropic triaxial stress that corresponds to the *in situ* stress model from the Swedish Forsmark site. The deposition tunnel is implicitly simulated by attaching the deposition borehole to a free domain boundary.

Several geomechanical cases are investigated, in which fracture growth is numerically evaluated as a function of *in situ* stress state, tunnel orientation, borehole geometry, total number of boreholes and borehole spacing. Numerical results show that spalling occurs in all cases, given the underground conditions at Forsmark, with borehole geometry, spacing and stresses affecting the extent of fracture nucleation and growth patterns.

The uncertainty in underground stress conditions is evaluated through varying stress magnitudes and orientations relative to the tunnel floor. Whereas tunnel orientation influences the relative locations where fractures initiate with respect to the tunnel floor, fracture growth and its final extent is determined by the relative magnitudes of the *in situ* stresses. Higher stress differential causes higher spalling depths, but in all cases, failure extent is localised to the borehole vicinity, not exceeding one borehole radius. The cylindrical borehole is modified at the top to provide an access ramp for the spent fuel canisters and fracture growth around several deposition boreholes is simulated for borehole tops having cylindrical, conical, and wedge shapes. The enlargement of the borehole top induces higher stress concentrations at the borehole–tunnel junction, increasing the severity of spalling closer to the tunnel floor. Massive failure occurs when a multiple borehole model is considered and the inter-borehole distance is small enough that adjacent “spalled” areas interact. At Forsmark, through-going fractures are predicted to develop when the borehole spacing is less than 4 m. The effect of spalling on the structural integrity of the deposition boreholes is illustrated for each test case and quantified in terms of maximum spalling depth, spalling width and total fractured surface area.

1. Introduction

Rock spalling is a type of stress-induced failure that occurs around deep excavation boundaries, in the absence of confining pressure. It often appears as rock splitting and flaky exfoliation in hard rocks, but can also manifest itself through shearing in weaker rocks.¹ Fairhurst

and Cook² first described spalling failure as the development of extension fractures (Mode I) under compressive loading. A number of early field studies^{3,4} have concluded that the direction in which spalling failure propagates is generally along that of the minimum compressive stress, and the final breakout can have different shapes such as “dog

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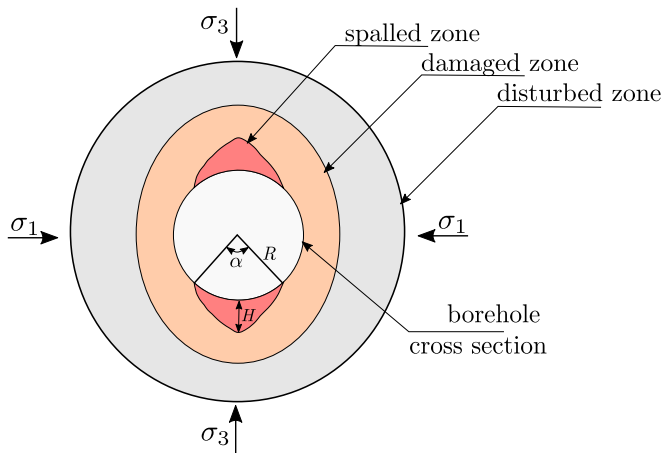


Fig. 1. Schematic radial cross-section of the three different zones of brittle failure around a circular excavation. The spalled, damaged and disturbed zones are stress/excavation-induced (modified from Read⁶). R , α and H denote the borehole radius, spalling angle (or spalling width), and spalling depth, respectively.

ear” or “cusp”,⁵ as shown in Fig. 1. Over the past few decades, spalling type failure leading to the development of V-shaped breakouts has been observed to occur at the centimetre scale in field studies involving deep underground experimental tunnels and boreholes, such as the Mine-by experiment tunnel run by the Atomic Energy of Canada Limited (AECL) Underground Research Laboratory⁷ and the Äspö Pillar Stability Experiment (APSE) conducted in the Hard Rock Laboratory (HRL) in Sweden.⁸ Several failure mechanisms have been proposed to explain the development of the spalled zone. Santarelli and Brown⁹ and Haimson and Song¹⁰ proposed that the failure mechanisms by which breakouts occur are different depending on the rock rheology. In hard rocks, spalling results from tensile fractures that initiate parallel to the borehole wall, while in weaker rocks it initiates at the borehole boundary and develops through shearing. Shen and Barton¹¹ emphasise that spalling may be the starting point of a breakout, but tensile failure is restricted to the immediate vicinity of an underground free surface, where extensional strain regime exists. Spalling has also been observed to occur in drilling experiments on centimetre scale cores for different rock types, including granites, limestones, and sandstones.¹²

1.1. Review of existing numerical models

Numerical methods for simulating rock fracturing can be divided into three main categories: continuum, discontinuum, and hybrid methods. Several spalling models have been implemented using continuum numerical methods and conventional plasticity theory.¹ Hajiabdolmajid et al.^{13,14} concluded that conventional constitutive laws such as elastic perfectly-plastic or elastic brittle-plastic cannot replicate the shape and depth of the spalled zone in strong rocks. The reason proposed for this is that in the conventional Mohr–Coulomb and Hoek–Brown criteria, the strength components (cohesion and friction angle) are assumed to be mobilised simultaneously. To overcome this limitation, Hajiabdolmajid et al.¹³ proposed modified versions of the Mohr–Coulomb and Hoek–Brown criteria, in which the cohesive and frictional strength components are functions of the accumulated plastic strain. Similarly, Diederichs¹⁵ used a cohesion-weakening frictional strengthening model with modified Hoek–Brown parameters to develop the Damage Initiation Spalling Limit (DISL) criterion.

Therefore, when implementing continuum approaches to model rock spalling, a modified constitutive criterion is usually required to capture the brittle rock response.^{13–16} While the cohesion weakening friction strengthening (CWFS) criterion used in continuum approaches generates results in agreement with the field observations, it requires

an *a priori* estimation of the plastic strain limits at which the cohesion component reaches its residual value and the frictional component becomes fully mobilised. Martin¹⁷ also highlights that the existing numerical studies using continuum methods can only provide an approximation of the fracturing process, which is inherently discrete. Another set of methods suitable for modelling borehole failure are the discontinuum numerical methods. Cundall¹⁸ provided a detailed account of discrete element methods (DEM) and their implementations, alongside their applicability in modeling discrete fracture initiation and growth.

An advantage of the discrete element method is that it does not depend on the previously mentioned constitutive criteria to define the fracturing process. Instead, particle contact¹⁹ is used to replicate brittle failure. The domain is discretised, each particle contact given a local, micro-strength and when the tensile or shear strength at the contact is overcome, it breaks. The progressive micro-failure process continues with subsequent bonds breaking, which then results in a stress redistribution around the domain and eventual macro-failure. A disadvantage of this method is that the given particle contact strengths, as well as particle shapes, sizes and clustering are not geomechanically informed, and so the accuracy of the fracture process cannot be verified.

Numerical studies using discontinuum methods showed that micro-scale heterogeneity²⁰, grain interlocking²¹, and grain clustering²² are important factors involved in tensile failure in hard, brittle rocks such as granites. Lan et al.²⁰ introduced the effect of small scale heterogeneities in a grain-based model, using the discrete element method (DEM). Polygonal grains of variable sizes were bonded together through contacts that accounted for the micro heterogeneity, but also allowed simulation of intra-element fracturing. Using a heterogeneous approach, Lan et al.²³ and Elmo and Stead²⁴ reproduced the depth and shape of spalling around the APSE pillar, and pillar strength, emphasising the importance of both grain-scale and large-scale heterogeneity in modeling borehole spalling.

The suitability of using a combined finite/discrete element method (FDEM) in order to simulate brittle fracturing has been investigated in several studies.^{25–29} This method is based on modelling fracture and damage processes explicitly, in order to simulate the transition from a continuous to a discontinuous domain, and hence replicate the fracture initiation and propagation more accurately. Vlachopoulos and Vazaios²⁵ recently simulated spalling damage using a combined finite-discrete element method (FDEM) in their model, and compared their results with the continuum DISL approach. Using the FDEM method, they were capable of simulating fracture initiation, coalescence, and propagation in two dimensions, as progressively larger stresses were applied to the borehole. Whereas discrete and hybrid methods have been shown to predict the spalled zone accurately to some extent, fracture growth is restricted to occur only along pre-existing element boundaries, where contact strength is initially assigned. In contrast, when fractures grow in geological media, their development influences the stress field in the vicinity of the crack, thereby affecting the fracture growth trajectories and the final fracture patterns, which end up being different from the predicted path.

The present study differs from those described above, as it investigates spalling in three dimensions, and applies a first-principle geometric fracture growth approach to model spalling/failure patterns. Fractures are modelled as distinct and explicit entities, and the volumetric discretisation of the rock domain is updated at every growth step to accommodate the evolving fracture geometry. Section 2 provides a comprehensive description of the methodology used.

1.2. The nuclear waste context

Deep underground disposal is widely considered one of the safer options for long-term disposal of nuclear waste. This type of disposal is based on a “multiple barrier concept”,³⁰ which introduces several protective layers between the disused fuel and the environment/society.

There are several on-going projects, at different completion stages, which investigate the engineered (e.g. type of canister, composition of the buffer, shape of the borehole), and the natural barriers (e.g. suitability of different host rock types, such as evaporites, crystalline rocks, argillaceous rocks, etc.), to determine their long-term behaviour and optimal configurations for safe repository design.

The Swedish Nuclear Fuel and Waste Management Company (SKB) has been undertaking site characterisation at Forsmark, where the Swedish deep geological repository for spent nuclear fuel is planned to be emplaced. The site is described extensively in SKB Technical Report SKB TR-08-05,³¹ which presents the integrated understanding of the bedrock geology, hydrogeology, rock mechanical and thermal properties, *in situ* stresses, and existing fracture domains and deformation zones.

The construction of a deep nuclear waste repository will involve excavating and drilling a range of underground structures of different geometries and sizes, such as access tunnels, deposition tunnels and deposition boreholes.³² One of the key issues that remains under investigation at Forsmark is the stability of the underground openings, and more specifically, the impact of stress-induced fractures on the deposition tunnels and boreholes, as well as their effect on the properties of the immediately-surrounding host rock. Understanding how over-stressed rock will behave following the creation of these voids will ensure an effective repository design and its long-term stability and maintenance.

Several telescopic boreholes drilled as part of the Forsmark site investigation work^{33,34} presented typical breakout patterns (Fig. 1), with widths and depths in the range of 40–180° and 0.2–67.5 mm respectively. These boreholes were percussion drilled with a diameter of cca. 200–250 mm in the first 100 m, and core drilled with a diameter of cca. 77 mm in the following 1000 m. Their dimensions and excavation methods are different from those of the future deposition boreholes,³⁵ which will accommodate the spent fuel, but the occurrence of spalling patterns in the investigation boreholes indicates possible stress-induced fracture growth impact on the deposition boreholes as well.

The main goals of this study are: (1) determine the effectiveness of the geometric fracture growth method in reproducing fracture mechanisms associated with mechanical spalling, and (2) model fracture initiation and growth following the excavation stage of underground openings in brittle rock formations. The Forsmark candidate area is chosen to investigate the impact of *in situ* stresses, deposition tunnel orientation, borehole geometry and spacing on the predicted breakout patterns. Since no deposition boreholes have yet been drilled at Forsmark, this paper also presents the first numerical predictions of fracture growth leading to spalling at the planned repository site.

2. Methodology

The present work models stress-induced spalling fractures geometrically, in an isotropic, homogeneous, and linear elastic brittle rock domain. Fractures are represented discretely using smooth parametric surfaces and their growth is evaluated at each fracture tip, where three modal stress intensity factors (*SIFs*) are calculated, capturing both tension and shear. *SIFs* represent the energy necessary to deform the fracture, and they determine the extent and orientation of growth vectors at every fracture tip.

The volumetric discretisation of the domain conforms to the evolving fracture geometry and is updated at each growth step to follow the new internal geometry. Fracture extension is thus not constrained by the mesh. Multiple fractures of different sizes and evolving shapes grow in response to the local stress state at their tips, with growth being influenced by the stress distribution within the domain, accounting for *in situ* stresses, fracture interaction, and variations in material properties due to ensuing damage.

This method has been applied in three dimensions in other studies, including fracture interaction,³⁶ hydraulic fracturing,^{37,38} and THM

fracture growth in the context of geothermal energy.³⁹ In particular, the present work presents extensions to the method that describe the nucleation of new fractures and the pervasive use of lofting to update geometric descriptions of fractures during growth. During the simulation, fracture geometry is always stored independently from the mesh,⁴⁰ and a new optimised mesh is created at every iteration at minimal computational cost using a fully parallel octree-based volumetric algorithm. The finite element method is used to compute the deformation in the system, taking into account the physical properties of both the rock domain and the fracture surfaces.

2.1. Displacement, stress and strain

Initial deformation of the intact rock domain is computed in response to the applied boundary stresses, as linear elastic deformation.⁴¹ For a homogeneous and isotropic rock matrix, the linear elastic stress-strain equation is

$$\sigma = D(\epsilon - \epsilon_0) + \sigma_0 \quad (1)$$

where σ and ϵ are the stress and strain tensors, σ_0 and ϵ_0 represent the initial stress and strain, and D is the linear elastic stiffness matrix, which accounts for the rock mechanical properties. Infinitesimal strain theory is then used to compute the strain corresponding to a given displacement gradient.⁴² In three dimensions, this can be written as

$$\epsilon = \frac{1}{2} [\nabla u + (\nabla u)^T] \quad (2)$$

Finally, Eqs. (1) and (2) are incorporated into an equilibrium equation that relates the forces and moment applied to the numerical domain, to the stresses that develop inside the domain:

$$\partial \sigma + F = 0 \quad (3)$$

2.2. Friction

Fractures are represented by a set of curve edges, which contains all the fracture tips, and two three-dimensional surfaces joined by the fracture edges. Fractures can either be open, with a varying aperture distribution, partially closed, or fully closed. The contact between the two fracture surfaces is resolved using a gap-based Augmented Lagrangian method with Uzawa algorithm adapted to fracture contact, that captures both stick and slip regimes across the fracture surface.⁴³

In the simulations presented in this paper, modelling of contact between fractures walls assumes friction $\mu = 0.45$, and utilises three internal, and three external Uzawa iterations, a total of nine iterations per cycle to resolve the effect of the friction and contact on the stress state. The friction coefficient was estimated by SKB from tilt tests at Forsmark,³² designed for fracture parameter determination according to Barton and Bandis.⁴⁴

2.3. Damage and fracture nucleation

Fracture nucleation is implemented based on a continuum damage model.⁴⁵ Damage mechanics is suitable to model spalling initiation, since this process occurs at micro-scale, where the rock is loaded beyond its elastic limit. After creating a borehole in a previously intact rock, the resulting stress concentration around the borehole creates a region of local degradation of the material, where fractures nucleate. This will generate a more realistic model of stress-induced brittle fracturing and a better representation of failure kinematics.²⁴

Fractures nucleate in the regions or elements with the largest damage, *i.e.* where the rock stiffness has been most degraded due to the stress redistribution around the borehole. The damage model of Mazars⁴⁵ is used, as this has been shown to predict quasi-brittle behaviour accurately. This is different from the linear-elastic equations

above, as a damage parameter d is introduced to scale the stiffness matrix.

Eq. (1) now becomes:

$$\sigma = (1 - d)D\epsilon \quad (4)$$

The damage parameter is found by calculating the equivalent strain, $\bar{\epsilon}$, which is the L^2 norm of the positive part of the strain tensor:

$$\bar{\epsilon} = \|\langle \epsilon \rangle\|_2 \quad (5)$$

Finally, the equivalent strain $\bar{\epsilon}$ is compared with a critical strain threshold, $\bar{\epsilon}_c$, which is the ratio of the rock tensile strength and the undamaged Young's Modulus, to decide if and where the fractures can nucleate:

$$\bar{\epsilon} \geq \bar{\epsilon}_c \quad (6)$$

2.3.1. Initial disk size

The initial flaw sizes are normally distributed and scaled with the amount of damage present at the nucleation location. Fig. 2 shows an example of nucleating 100 initial fracture disks around a circular borehole, where (A) all of them have uniform initial radii of 50 cm, (B) the initial sizes follow a normal distribution, with 50 cm mean radius and 20% of the mean radius as standard deviation and (C) the initial sizes are normally distributed as in the previous case, but also scaled according to the damage distribution.

The largest disk size, corresponding to the highest damage present in the rock, is set to 50 cm and decreases as shown in Fig. 2C, taking as a maximum value the size of the element in which damage is computed. Before nucleation, the area surrounding the borehole walls was refined to enforce a minimum element length of 0.5 m, which reduces as fractures are created and their tips are refined. During nucleation, fractures of radius consistent with the size of their corresponding nucleating elements are created. This translates to initial fractures of 0.5 m maximum radius, reducing element size to $2 \times \pi \times 0.5 / 12 = 0.26$ m, for 12 tips per fracture, and subsequently reducing as fractures get inserted with a minimum radius of 0.01 m.

Thus, for a damaged element in which a fracture is being nucleated, the maximum diameter of the fracture will be given by the maximum length of the element in the plane defined by the fracture normal. This can be adjusted to be made to always be contained by the element, and can be adjusted to be a pure material property. A fixed fracture length, consistent with changing element sizes, is implemented throughout the presented simulations, as it better captures spalling fractures that nucleate closest to the borehole wall, while also capturing smaller fractures that nucleate away from the borehole in zones of lower damage, that may not be directly involved in spalling or splitting of the borehole wall (Fig. 2C).

2.3.2. Number of initial disks

The number of initial seeds is the maximum number of fractures that can nucleate during a simulation. When the damage criterion is not satisfied, additional fractures will not nucleate, regardless of whether the maximum number of fractures has been reached or not. Fig. 2 shows an example of nucleating 100 initial fracture disks, with the different sizing strategies explained above. The present approach does not aim to numerically model growth of every individual fracture that occurs at the borehole wall. Instead, flaws are geometrically modelled in locations where the damage is the highest.

The initial fracture disks are prevented from nucleating on the surface of the borehole wall, but there is no artificial restriction on growth, as the fractures are allowed to evolve following fracture mechanics principles (see Section 2.4). The nucleation process is in line with the earliest two stages of spalling observed by Haimson¹² on cm-scale Westerly granite cores, who described spalling initiation as a three-stage process: (a) growth of tensile micro-cracks (Mode I), subparallel

to σ_1 , and along σ_3 ; (b) interaction and coalescence of these micro-cracks into clusters, delineating rock slabs, and (c) intersection with the free borehole surface and buckling of the slabs.

The nucleation method used in this paper captures the sequential nature of the breakout development partially. There are no micro-fractures forming at the borehole wall that coalesce and buckle in a progressive manner. Instead, a damage model is used to determine the loci of fracture initiation. The final shapes and depths of failure are suitably reproduced (Section 4), after growth and interaction of the macro-fractures is completed. Since fracture growth is determined by the energy at the fracture tip, each nucleated fracture disk will extend to accommodate the damage around it until it either intersects a free surface, or ends up in the stress shadow zone of another fracture, which had already accounted for the damage present in its respective neighbourhood.

Intersections between fractures and the free borehole surface are handled using 3D Boolean operations. When a fracture reaches the borehole wall, the fracture is split by the borehole wall and the parts that reside inside the borehole are discarded. Further growth into the borehole is handled by defining a high K_{IC} for the borehole material, which prohibits tip growth inside the borehole.

2.3.3. Damage of the rock domain

The Young's Modulus in the model is modified based on the concept of strain equivalence of Lemaitre,⁴⁶ who proposed that the damage state may be described through an elastic modulus change of the material:

$$\tilde{E} = E(1 - d) \quad (7)$$

where d is the damage variable from Eq. (4), and $\tilde{E}$ is the damaged Young's Modulus. In the numerical simulations, Young's Modulus is stored as a constraint point variable, with one value computed per integration point of the element. This captures variation of the properties within the volumetric element, and ensures a continuous loss of rock strength across the element. Therefore, the variation of the material properties occurs at a finer level than the actual dimension of the mesh. For visualisation purposes, throughout this paper, Young's Modulus is extrapolated to the element to simplify its graphical representation.

To ensure the numerical stability of the simulations, the minimum value of Young's modulus is capped at a small non-zero value (for most simulations herein, 10 Pa), which effectively corresponds to a substantial reduction of the elasticity modulus, meaning that the rock has completely lost its integrity. The damage variable d is calculated using the Mazars⁴⁵ damage model. For a general stress state, the damage parameter is defined as a linear combination of damage under tension and under compression:

$$d = \alpha_t d_t + \alpha_c d_c \quad (8)$$

where α_t and α_c account for tensile and compressive stress states, respectively. These coefficients are evaluated as

$$\alpha_t = \left(\sum_{i=1}^3 \frac{\epsilon_{ti} \langle \epsilon_i \rangle}{\bar{\epsilon}^2} \right)^\beta \quad \text{and} \quad \alpha_c = \left(\sum_{i=1}^3 \frac{\epsilon_{ci} \langle \epsilon_i \rangle}{\bar{\epsilon}^2} \right)^\beta \quad (9)$$

where ϵ_{ti} and ϵ_{ci} are the principal strains due to positive and negative stresses, respectively, and the exponent β is a damping factor, here assumed to be equal to 1. Both tensile and compressive parts of the damage are computed from the same equivalent strain (Eq. (5)), using the following equations:

$$d_t = \begin{cases} 0, & \text{if } \bar{\epsilon} \leq \epsilon_0 \\ 1 - \frac{(1-A_t)\epsilon_0}{\bar{\epsilon}} - A_t e^{-B_t(\bar{\epsilon}-\epsilon_0)}, & \text{if } \bar{\epsilon} \geq \epsilon_0 \end{cases} \quad (10)$$

$$d_c = \begin{cases} 0, & \text{if } \bar{\epsilon} \leq \epsilon_0 \\ 1 - \frac{(1-A_c)\epsilon_0}{\bar{\epsilon}} - A_c e^{-B_c(\bar{\epsilon}-\epsilon_0)}, & \text{if } \bar{\epsilon} \geq \epsilon_0 \end{cases} \quad (11)$$

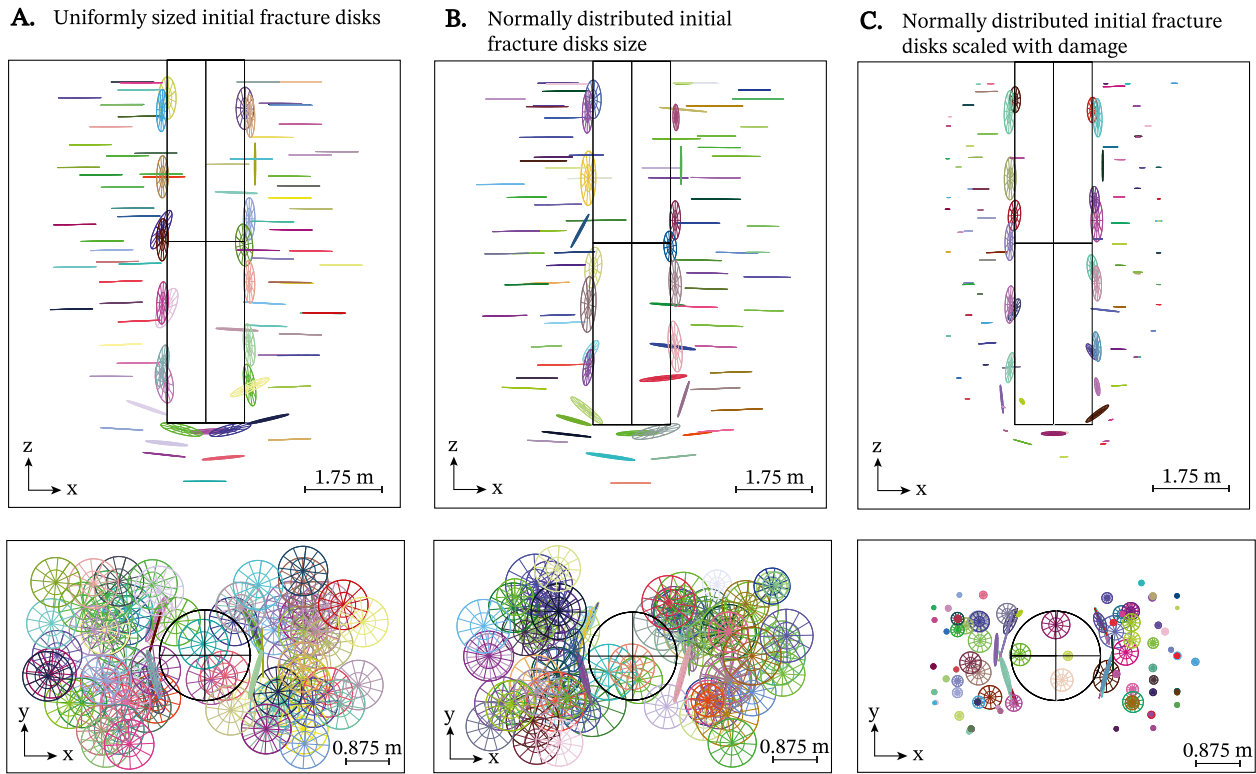


Fig. 2. Nucleation of 100 fractures in the first 100 most damaged elements around the borehole. Each fracture disk radius is 50 cm (A). Improved fracture nucleation patterns are obtained by defining a normal distribution of initial disk radii (B), and scaling their radii according to the damage in the element, which varies as a function of the distance away from the borehole (C).

where ε_0 is the strain threshold and A_t , B_t , A_c , and B_c are material parameters that determine the post peak shapes of the uniaxial stress-strain curves in tension and compression, respectively. A_t and A_c affect the residual strength component of the curve, and B_t and B_c modulate the slope of the curve in the strain-softening region.

The four material parameters applied in the present work (Table 1) are numerically computed by fitting stress-strain curves to data obtained by SKB through uniaxial compression⁴⁷ and indirect tensile⁴⁸ tests on pegmatite samples from Forsmark. Although the main repository host rock at Forsmark is a granodiorite, the pegmatite approximation should be sufficient. This can be confirmed in future analysis when relevant data from experimental tests on granodiorite becomes available. Souissi et al.⁴⁹ showed that three different rocks (a granite, a limestone, and a sandstone) may have identical Mazars damage parameters, since the brittle stress-strain response of the three rocks in the post-failure stage is similar.

2.4. Fracture growth

Based on the knowledge of the stress and displacement fields, fractures are set to nucleate when the damage exceeds the critical strain threshold. In order for these to grow, in addition to the stresses and displacements, *stress intensity factors* (SIFs)⁵⁰ are calculated at the fracture tips for the three principal deformation modes (K_I , K_{II} , and K_{III}).

Numerically, SIFs are calculated using the J-integral, evaluated at each fracture tip using the virtual integration disk method.^{51,52} This method computes the I-integral numerically over a two-dimensional virtual disk domain, providing the strain energy release rate, G , from which the values of K_I , K_{II} , and K_{III} are extracted.⁵³ Alternatively, if the fractures are too close to one another, SIFs can be calculated from the displacement field, using the displacement correlation and the quarter point technique^{54,55} to increase displacement accuracy around the fractures tips.

Computed SIFs are incorporated into a 3D mixed-mode criterion⁵⁶ that evaluates the projected stress intensity factor (K_v) onto the local coordinate system of the fracture. This criterion enables fracture growth when K_v exceeds the material toughness:

$$K_v = \frac{K_I}{2} + \frac{1}{2} \sqrt{K_I^2 + 4(\alpha_1 K_{II})^2 + 4(\alpha_2 K_{III})^2} \geq K_{IC} \quad (12)$$

where K_{IC} is the critical stress intensity factor for Mode I fractures, or material toughness, and α_1 and α_2 represent the material toughness ratios of mode I to mode II (1/1.3268) and mode I to mode III respectively (1/0.6297). These values have been determined by Chang et al.⁵⁷ and Aliha et al.⁵⁸ for different materials. Fracture tips that have met the failure criterion will extend by a certain length, and in a certain direction. Extension length is calculated using a formulation of the Paris-type extension criterion proposed by Thomas et al.⁵⁹:

$$\Delta l_f = \Delta l_v \left(\frac{G_f}{G_v} \right)^{\beta_f} \quad (13)$$

where G_f is the fracture energy release rate, corresponding to the largest value of G_n on fracture f ; G_n is fracture tip energy release rate, calculated at each fracture tip node; G_v is the largest value of G_f in the fracture domain; Δl_v is maximum extension length per growth step from all fractures in the domain and Δl_f is maximum individual fracture extension. The second extension length is calculated once per fracture tip n :

$$\Delta l_n = \Delta l_f \left(\frac{G_n}{G_f} \right)^{\beta_n} \quad (14)$$

where Δl_n is the extension of a local tip and G_n is the energy release rate at tip n . β_f and β_n correspond to fracture and tip growth rate exponents respectively. Since different β_f and β_n values influence how individual tips and interacting fractures grow with respect to each other, several cases were tested to determine which values of the growth exponents generate representative spalled zones geometries in both radial and

axial directions. Throughout this paper $\beta_f = 0.25$, $\beta_n = 0.75$ and $\Delta l_v = 0.4$ m.

At each fracture tip location, an angle criterion⁵⁶ is applied to determine the direction in which the crack will extend. This criterion employs the three SIFs as follows:

$$\phi = \pm \left[A \frac{K_{II}}{K_I + K_{II} + K_{III}} + B \left(\frac{K_{II}}{K_I + K_{II} + K_{III}} \right)^2 \right] \quad (15)$$

where $A = 140^\circ$ and $B = -70^\circ$. The angle criterion has been validated by Richard et al.,⁶⁰ who compared the numerical predictions with experimental data for a number of materials and boundary conditions, and concluded that they can be applied to predict fracture growth accurately.

Implemented growth criteria allow the numerical model to capture deformation on each of the tips along the fracture front, so these can grow in different directions and magnitudes in response to the local stresses.

2.5. Fracture geometry, NURBS, and lofting

The fracture geometry consists of an ensemble of points, curves and surfaces, and it evolves as a function of deformation. Fractures are represented through non-uniform B-splines (NURBS) curves and surfaces. NURBS have been increasingly used in representing fracture domains,⁶¹ because they can accurately describe a wide range of shapes: from simple 2D lines and curves, to more complex 3D free-form surfaces and volumes. Piegl and Tiller⁶² give a general overview of NURBS and Paluszny et al.⁶³ show the advantages of using NURBS in the context of fracture mechanics with explicit geometric representation of fracture shapes.

Fig. 3A shows an example of fracture growth under uniaxial tension from an initial smooth disk, for a total of five growth steps. In this case, the fracture surface is constructed by connecting the old (prior to growth) and new (after growth) sets of fracture tips through triangles. During the first growth step, the initial smooth crack boundary becomes a set of polylines, connected at the fracture tips. This process causes a high density mesh (Fig. 3C) to be generated around the fracture, to conform with the evolving fracture geometry. An improved construction of the geometry of the fracture surface is through lofting (Fig. 3B), which results in a coarser mesh of high quality that provides a smooth representation of the geometry, while maintaining fidelity to the fracture geometry.

The lofting process is illustrated in Fig. 4. At each growth step, a smooth NURBS curve, representing the fracture front, is constructed through the newly computed fracture tips. A NURBS curve of degree p is a piece-wise polynomial curve, that can be represented in the parametric $u-v$ space as:

$$C_k^w(u) = \sum_{i=0}^n N_{i,p}(u) P_{i,k}^w \quad (16)$$

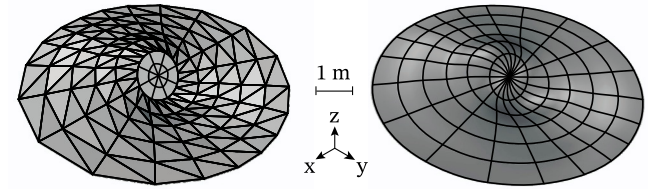
where $P_{i,k}^w = (w_{i,k}x_{i,k}, w_{i,k}y_{i,k}, w_{i,k}z_{i,k}, w_{i,k})$ are the weighted control points of the curve and $N_{i,p}(u)$ are the basis functions of the curve. All the basis functions are defined over the same knot vector $U = \{a, \dots, a, u_{p+1}, \dots, u_n, b, \dots, b\}$, where a and b are usually 0 and 1, respectively. The successive NURBS curves representing the fracture front at every growth step represent the section curves of the fracture surface.

The lofting technique consists of mathematically blending the section curves together to form a smooth surface.⁶⁴ Similar to the definition of a NURBS curve, a NURBS surface of degree $p \times q$ in the $u-v$ parametric space is commonly defined as:

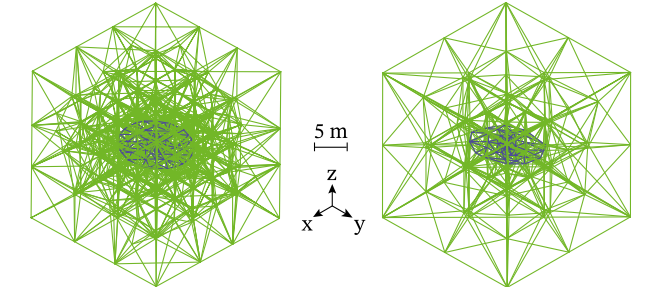
$$S^w(u, v) = \sum_{i=0}^n \sum_{j=0}^m N_{i,p}(u) N_{j,q}(v) P_{i,j}^w \quad (17)$$

where $P_{i,j}^w = (w_{i,j}x_{i,j}, w_{i,j}y_{i,j}, w_{i,j}z_{i,j}, w_{i,j})$ are the control points of the surface, and $N_{i,p}(u)$ and $N_{j,q}(v)$ are the basis functions of the

A. Fracture geometry (triangles) B. Fracture geometry (lofting)



C. Mesh (fracture + domain) corresponding to A. D. Mesh (fracture + domain) corresponding to B.



Domain volume elements : 4334	Domain volume elements : 2252
Fracture surface elements: 218	Fracture surface elements: 185
Total elements : 5022	Total elements : 2707
Total nodes : 6425	Total nodes : 3422

Fig. 3. Fracture geometries (A, B) and corresponding mesh densities (C, D) after five growth steps, when the fracture surface is constructed through triangles (A) and lofting (B). Triangular fracture surface elements are shown in blue, and tetrahedral volume elements are shown in green. In both cases, the fracture has 20 tip segments, i.e. 21 tip nodes. (For interpretation of the references to colour in this figure legend, the reader is referred to the web version of this article.)

surface. The two sets of surface basis functions are defined over their corresponding knot vectors.

The geometric process of lofting consists of performing curve interpolations across the control points of the section curves, resulting in the control net of the surface. Control points do not necessarily always lie on the physical location of those of the curves, but rather describe their domain through a series of recursive operations that translate a local reference space into a curve or surface that exists in physical space.

The newly constructed surface is called a lofted surface, as it lofts or approximates the region between a pair of two curves, each described by a set of control points of equal size. These cross-sectional curves become isoparametric curves of the lofted surface as shown in Fig. 4. The final surface shape will depend on the number, shape and positioning of the section curves, and will evolve as in the natural system by adding new surfaces to the existing surface object. Piegl and Tiller⁶² provide an in-depth description of NURBS geometries and relevant operations.

In the context of modelling fracture growth in nuclear waste repositories, NURBS representations of fractures are advantageous for several reasons: they capture details at many scales, since they are not limited by a specific resolution; they do not require large storage memory, which would be problematic if hundreds of fractures are generated; they are consistent with emerging isogeometric methods. The gains from representing complex geometries using parametric surfaces, such as NURBS, are substantial, allowing for substantially higher quality meshes to be generated in an efficient manner for their use in both mechanics and flow numerical experiments.⁶⁵

3. Experimental setup

The borehole and tunnel geometries used in this paper are those currently planned for the final repository⁶⁶ that will be constructed at the Forsmark site. The *in situ* stresses and rock properties are also representative of those encountered at Forsmark,^{31,35} with the exception

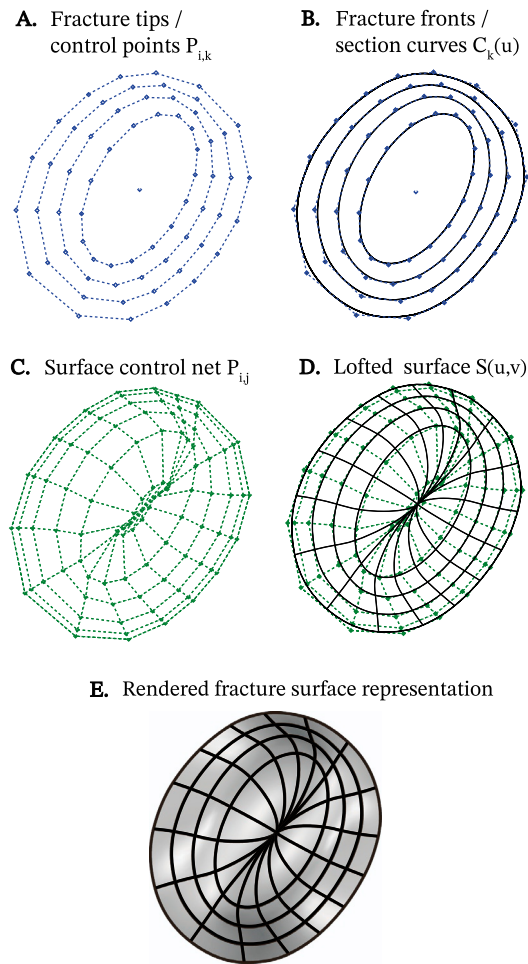


Fig. 4. Schematic representation of the lofting process showing four growth steps in a single fracture. The four successive fracture fronts are constructed through 16 control points ($P_{i,k}$), which correspond to the tips nodes of fracture i at growth step k (A). The fracture fronts are represented through NURBS curves ($C_k(u)$) in parametric space (B). The control net of the fracture surface ($P_{i,j}$) is obtained by interpolating in the direction of fracture growth between the fracture fronts at successive growth steps (C). Finally, the fracture surface ($S(u,v)$) is constructed through the control net in parametric space (D, E).

of Mode I fracture toughness, which corresponds to the Äspö diorite.⁸ There are currently no measurements of fracture toughness available at Forsmark, but SKB is conducting tests to estimate it. The influence of the new values will be analysed when the new data becomes available. Several cases are considered:

- Borehole model (Fig. 5A): a 20 m × 20 m × 20 m block model, containing a 1.75 m diameter circular vertical borehole, subjected to a far-field anisotropic triaxial stress. Far-field stresses σ_v , σ_h and σ_H are applied on the top, left and front domain surfaces respectively, and displacements are fixed, for numerical convergence, on the faces and edges symmetrically opposite to the faces and edges where the stresses are applied. Roller boundaries (circle symbols) show the direction in which outer nodes can move freely. The corner node where three constrained boundaries intersect one another is constrained in all three directions (triangle symbol). Inner nodes belonging to the borehole surface are not constrained. This scenario was altered for the validation case (Section 4), where σ_h and σ_v were flipped, to simulate the spalling in the roof and the floor of a horizontal tunnel.
- Borehole and tunnel model (Fig. 5B): Similar to the simple borehole model, but the borehole is attached to a “free” boundary

simulating the effect of the deposition tunnel. The “top” domain boundary is split into three surfaces and no vertical forces ($\sigma_v = 0$ MPa) are applied on the nodes belonging to the tunnel floor. The “free” surface is illustrated as a 4.2 m wide red-coloured “stripe”, which corresponds to the width of the deposition tunnel floor. The rest of the “top” boundary is subjected to vertical stress, as triaxial stress conditions return away from the tunnel floor. This was shown to model the effect of the deposition tunnel on the stress distribution, without requiring to model the tunnel explicitly in subsequent simulations, resulting in faster runtimes.⁶⁷

- Multiple borehole model (Fig. 5C): The same as the borehole and tunnel model, with additional boreholes on either side of the central borehole, aligned along the tunnel axial direction. Several borehole spacings are tested, on a 3-borehole model, as well as the effect of increasing the number of boreholes in the model to five and seven.
- Modified borehole model (Fig. 5D): The 1.75 m diameter circular borehole is enlarged at the top, in a cone shape and a wedge shape, respectively. Modified borehole geometry would facilitate the emplacement of the spent nuclear fuel canister.^{32,68}

4. Validation

In this work, mechanical spalling is simulated in three dimensions using the Imperial College Geomechanics Toolkit (ICGT), a finite-element-based discrete fracture growth simulator written in C++. The ICGT has been extensively validated for fracture growth^{59,69} and fracture interaction^{36,59} in three-dimensions, as well as for friction and contact⁴³ and flow and permeability.⁷⁰ In this section, the present fracture growth methodology implemented in ICGT is validated against the URL Mine-By Experiment, for mechanical spalling.

The Atomic Energy of Canada Limited (AECL) carried out the Mine-by tunnel experiment⁶ in order to characterise spalling around a circular borehole under high stresses. The experiment consisted of drilling a 3.5 m diameter circular tunnel in a relatively unfractured granitic rock. The tunnel was excavated subparallel to the intermediate principal stress direction σ_h , in order to maximise the difference between the minimum and maximum compressive stresses acting on the tunnel boundary. Spalling became apparent in both the roof and the floor of the tunnel (diametrically opposite) after the borehole was cleaned and the spalled flakes removed through scaling (Fig. 6A). The maximum depth of the spalled zone was approximately 50 cm, with variations along the tunnel length, and the rock mass remained intact beyond this point.

The numerical model pertaining to the validation case was set up using the rock properties of the Lac du Bonnet granite, and far-field stresses corresponding to the *in situ* conditions at the 420 Level: $\sigma_H = 55 \pm 5$ MPa, $\sigma_h = 48 \pm 5$ MPa, and $\sigma_v = 14 \pm 1$ MPa.⁷²

Fig. 6B and C show the final fractured zone geometry, after the spalling process has stabilised and fracture growth has arrested, in radial and axial view respectively. Each macro-fracture that interacts with the borehole wall can be regarded as a “fractured zone”. When compared with other numerical studies, which model the progressive splitting of the borehole wall through coalescence of smaller fractures,^{3,5} or through propagation of active damage planes,⁷³ the fractures shown in this paper represent the final stage of failure and they inherently encompass all the intermediate stages.

Spalling extent is given by the fractured region around the borehole. For each fracture surface, the “spalling depth” is the maximum distance measured between fracture tip nodes and the borehole wall, and the “spalling width” is the maximum arc length of that fracture around the borehole, as seen from an observation point at the top of the borehole. The maximum measured depths and widths of the two fracture arrays forming the diametrically-opposite notches (Fig. 6B) are consistent with those observed in the field (Fig. 6A). Although a relatively small number of fracture disks are initiated, i.e., 10, they are allowed to grow

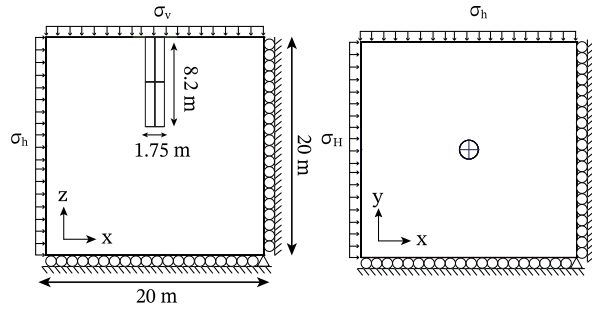
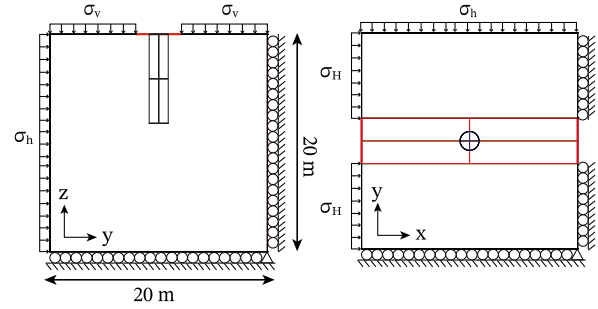
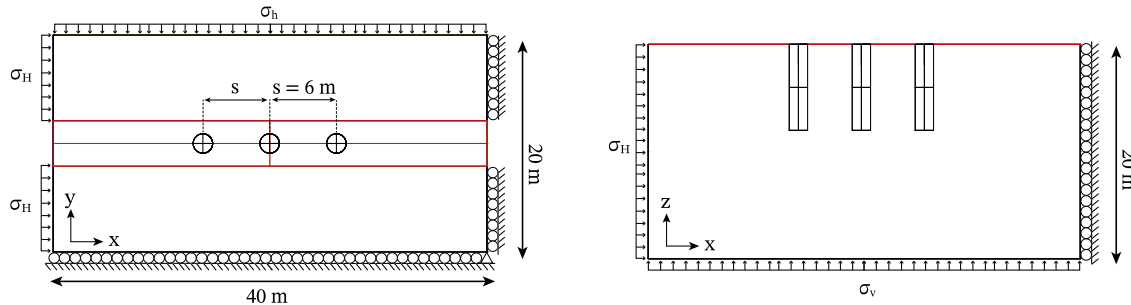
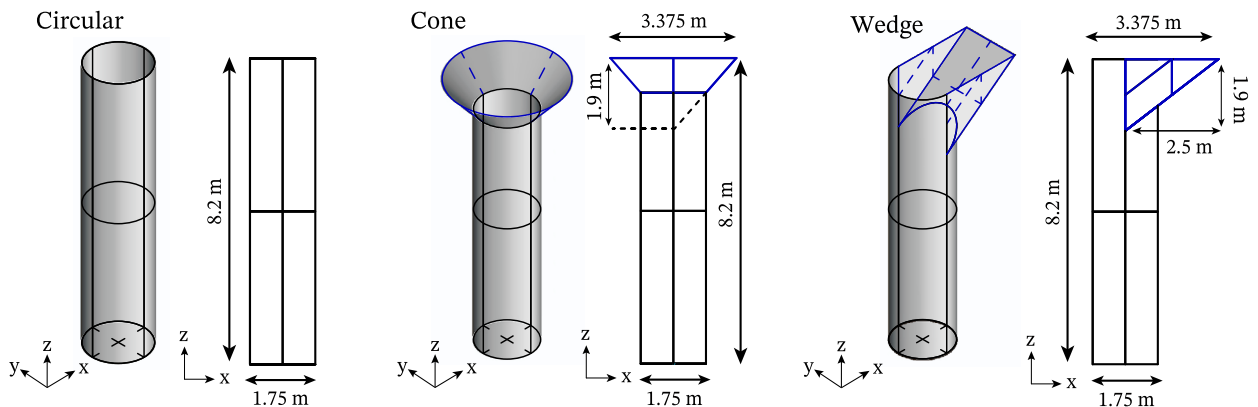
A. Borehole Model**B. Borehole and Tunnel Model****C. Multiple Borehole Model****D. Borehole Geometries**

Fig. 5. Model geometry and boundary conditions for the different simulated cases: (A) Simple borehole model under triaxial load, (B) Borehole and tunnel model: same geometry as (A), but modified boundary conditions at the top domain boundary, such as the deposition tunnel floor (red-coloured stripe) is a free surface. (C) Multiple borehole model: same boundary conditions as (B), but the domain is enlarged in the direction of the tunnel floor to avoid lateral boundary effects. (D) Variations of the borehole top geometry, used in subsequent simulations. In all cases, σ_h and σ_H are the minimum and maximum horizontal stresses respectively, and σ_v is the vertical stress, corresponding to the weight of the overburden at 500 m depth at Forsmark. (For interpretation of the references to colour in this figure legend, the reader is referred to the web version of this article.)

freely, based on the energy in the system, eventually covering the entire length of the borehole and accommodating the whole range of damage that develops in the near-borehole region after the borehole drilling stage.

Rock buckling and collapse into the borehole are not explicitly modelled, but the loss of integrity of the borehole is accounted for through damage. This is illustrated in Fig. 7, where the elastic modulus of the host rock is degraded (Eq. (7)) in the vicinity of the borehole, amounting to a 60% reduction in the intact rock elastic modulus (Fig. 7A). Once fractures nucleate, Young's Modulus is reduced completely in the elements neighbouring the newly spawned fracture surfaces (Fig. 7B). Further degradation of the rock mass occurs as fractures grow (Fig. 7C), covering the whole length of the tunnel. As opposed to the field experiment where the two diametrically opposite spalled zones were asymmetrical, the fractured zones in the numerical model tend to be symmetrical, due to the prescribed homogeneity and isotropy of the numerical domain.

To achieve the fracture patterns shown in Fig. 6, several parameters were calibrated for nucleation and growth: number of initial disks = 10; initial disk size = 30 cm, following a normal distribution and scaled with damage, cf. Fig. 2 C; minimum fracture spacing = $1.5 \times$ initial disk size; maximum extension length $\Delta l_v = 40$ cm per growth step; $\beta_f = 0.25$; $\beta_n = 0.75$. These parameters are used throughout the paper, in the subsequent simulations of spalling at the Forsmark site.

5. Effect of the deposition tunnel

The deposition tunnel is expected to affect fracture nucleation and growth patterns, through changing the stress state at the top of the deposition boreholes, where it acts as a free surface. Saceanu et al.⁶⁷ showed that the same tunnel effect can be modelled in two ways: (a) by explicitly modelling the deposition tunnel as an individual body in a larger domain, or (b) using modified boundary conditions on the

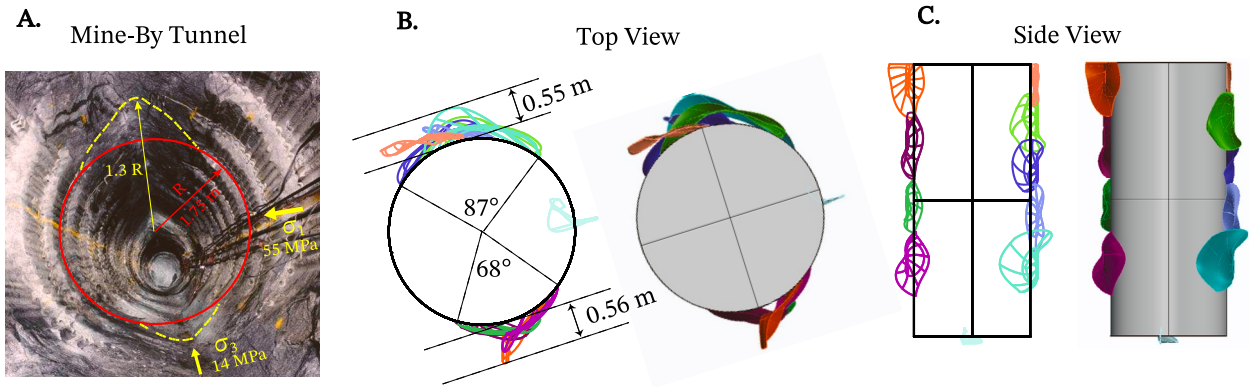


Fig. 6. Spalled zone around the AECL's 3.5 m diameter tunnel, modified from Zhao et al.⁷¹ (A), and numerical simulation results showing radial (B) and axial (C) views of the final fracture patterns.

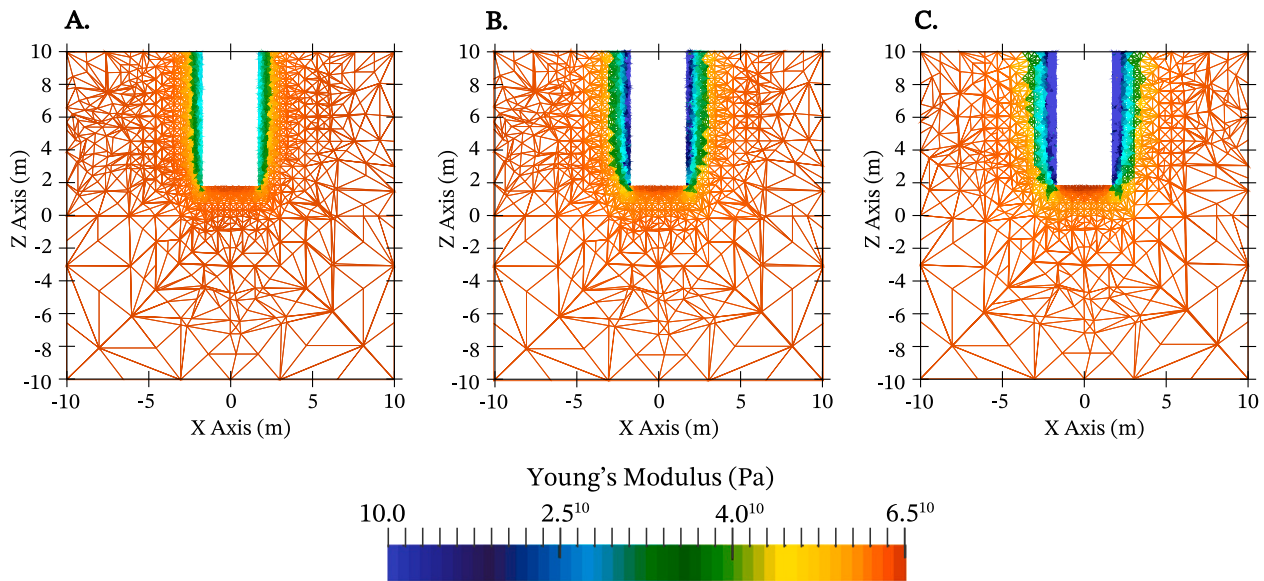


Fig. 7. Mesh cut planes showing Young's Modulus distribution along AECL's circular 3.5 m diameter Mine-by tunnel, after the tunnel is drilled (A), after fracture nucleation (B) and after fractures have stopped growing (C). (For interpretation of the references to colour in this figure legend, the reader is referred to the web version of this article.)

Table 1

Rock properties,³¹ damage parameters and *in-situ* stress magnitudes³⁵ defined for the numerical simulations of deformation, fracture and damage of the Forsmark case.

Rock property	Value
Density	2.73 g/cm ³
Young's modulus	76 GPa
Poisson's ratio	0.23
Uniaxial compressive strength	226 MPa
Tensile strength	13 MPa
K_{IC}	3.8 ^a MPa/m ^{1/2}
<i>In situ</i> stresses	Value
σ_H	41.0 ± 6.2 MPa
σ_h	23.2 ± 4.6 MPa
σ_v	13.2 ± 0.3 MPa
Mazars damage parameters	Value
ϵ_0	3.2 × 10 ⁻⁴
A_c	0.73
B_c	1816
A_t	0.73
B_t	1816

^aMode I fracture toughness value of the Äspö diorite, taken from the APSE experiment,⁸ as there are currently no measurements of fracture toughness at the Forsmark site.

top surface of the domain and creating a “free” boundary where the deposition borehole is attached. This change has a minimal effect on the simulated fracture geometries, but is computationally more efficient, since it employs a simplified domain, with a reduced number of nodes and elements. The resulting stress fields around the borehole were shown to be nearly identical in the two models where either the whole tunnel, or the tunnel floor, was simulated.⁶⁷

Fig. 8 shows the damage (Eq. (8)) distribution in a full borehole and tunnel model, following the nucleation of the initial fracture disks. The tunnel is excavated in one step and the displacements, strains and stresses are computed in the model containing only the tunnel. Subsequently, the borehole is drilled in one step, and the stresses are re-computed in the full borehole and tunnel model. Fig. 8 A–C shows separate instances of preferred nucleation loci in the full model containing 10, 50 and 100 initial disks respectively. The highest damage concentrations occur at the intersection of the tunnel floor and its sidewalls and at the intersection of the deposition borehole wall and the wedge-shaped access ramp. These results are in line with experimental observations where fractures nucleate preferentially around sharp-edged boundaries.⁷⁴

Numerically, first fractures to nucleate are those at the edges of the tunnel floor (Fig. 8A), followed by spalling of the borehole top/access ramp (Fig. 8B), and finally spalling of the lower curvature surfaces

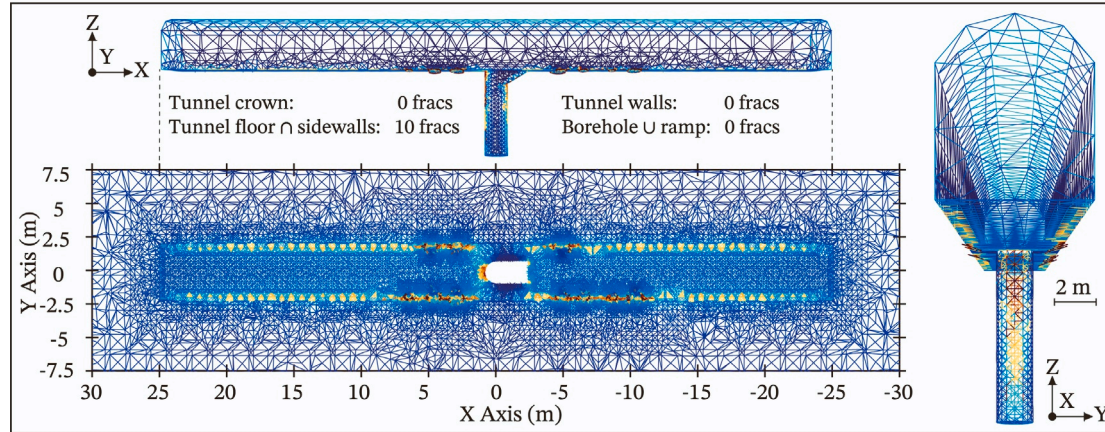
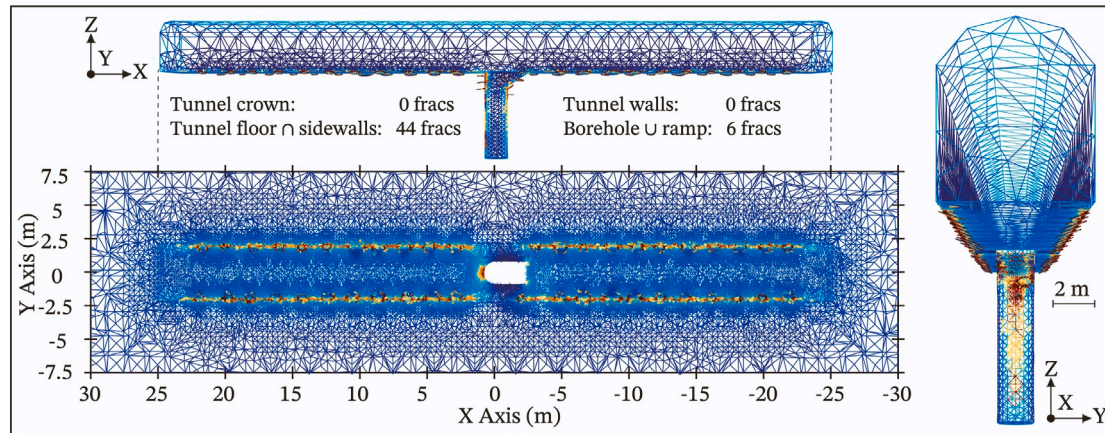
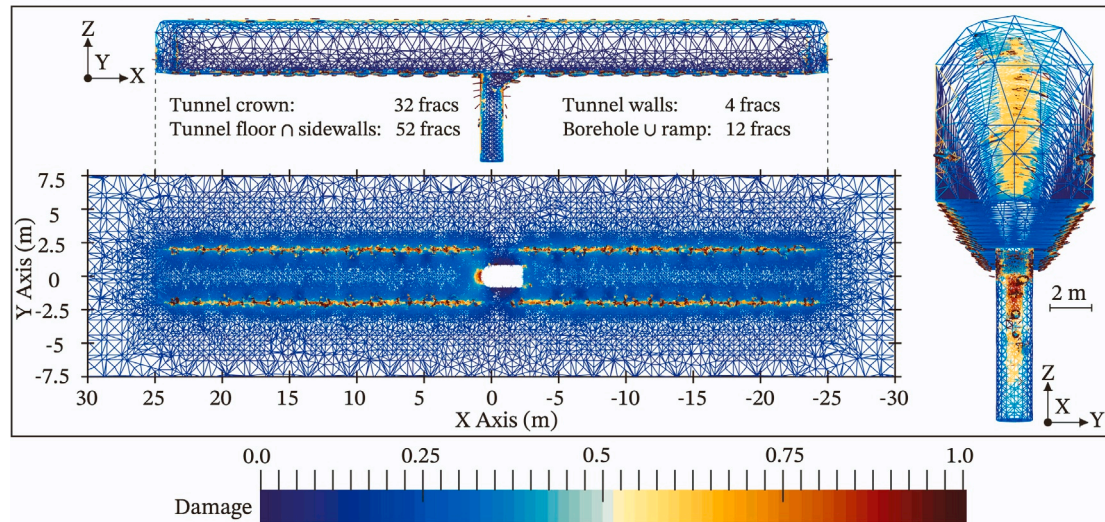
A. Nucleation: 10 fractures**B. Nucleation: 50 fractures****C. Nucleation: 100 fractures**

Fig. 8. Damage and initial fracture disks distribution in the full borehole and tunnel model. First fractures to nucleate are at the tunnel floor/sidewalls (A), followed by those at the borehole top (B), and finally those at the tunnel roof and lower borehole walls (C). The tunnel axis is oriented perpendicular to the maximum *in situ* stress direction. Simulation parameters and boundary conditions are those from Forsmark (Table 1).

such as the tunnel roof and the rest of the borehole wall (Fig. 8C). This is also an example of the complexity of a full borehole and tunnel model, which requires hundreds of fractures to capture the complete state of the damage present around both cavities. In this paper, only the fractures directly involved in the development of the borehole breakouts/borehole spalling are further analysed. Consequently, the

single-step borehole excavation is considered to be representative of the subsurface behaviour, given that observed fractures take significantly longer time (4–8 months⁷) to develop the v-shaped notches in the field, compared to 30–105 h⁷⁵ required to complete a typical deposition borehole.

Fig. 9 shows the minimum principal stress distribution in the 20 m $\times$ 20 m $\times$ 20 m single-borehole model, with (A) and without (C) tunnel effect, after the borehole drilling stage. In the close vicinity of the tunnel floor, the triaxial compressive conditions become biaxial, due to the removal of the vertical stress. Borehole geometry and initial fractures disks that ensue after nucleation are superimposed, showing fractures nucleating preferentially parallel to the tunnel floor in the first 2 m below it, and changing their nucleation orientation further down the borehole. As σ_3 increases further away from the tunnel floor and triaxial conditions return, fractures align themselves perpendicular to the minimum compressive stress direction, and hence parallel to the borehole wall.

Fig. 9B and D show the final fracture geometries after growth has arrested, in the two cases. In the case where the tunnel floor is considered, the top fractures grow to a lesser extent in the borehole axial direction, and there is overall less interaction with the borehole wall and between the fractures themselves. Instead, fractures tend to propagate in the plane of the tunnel floor, appearing to increase spalling depth. However, since there is little growth along the borehole wall, the actual fractured surface area is smaller than in the case where the tunnel floor is not considered.

Fig. 9B and D also shows fracture tip nodes at all growth steps, coloured according to their dominant failure mode. In both cases, most tips appear to fail under tension in the early stages of the breakout development, but further fracture propagation is mixed mode, and mainly determined by either Mode II or Mode III tip failure. As expected, in the case where the deposition tunnel is considered, more tips fail in tension, since decreasing confining stress promotes tensile fracture conditions.

5.1. Effect of tunnel orientation

Several cases were investigated, in which the tunnel floor was orientated axially parallel to the direction of the maximum horizontal far-field stress, and then rotated in 15 degree increments, until it became perpendicular. Fig. 10B illustrates simulated fracture patterns in three different cases, in which the deposition tunnel makes an angle of 0°, 30° and 45° respectively with σ_H . The computed displacement and stress field around the borehole change in direction, at fixed stress magnitudes. Results show that tunnel orientation influences the loci where the fractures involved in borehole spalling nucleate, relative to the tunnel floor, but does not influence the depth of the final fractured areas significantly.

The spalling depth, spalling width, and total fracture surface area in each case are listed in Table 2. These remain consistent at different tunnel orientations, because the tunnel floor edges are sufficiently far away from the borehole wall to prevent unstable fracture growth and interaction with tunnel floor/sidewalls. Given the *in situ* conditions at Forsmark, spalling is not predicted to occur in the tunnel axial direction, unless the tunnel is drilled perpendicular to the maximum horizontal stress (Fig. 10A). Section 8 shows further examples, deliberately probing this worst-case tunnel orientation scenario, to understand the behaviour for cases where fracture growth and interaction between neighbouring boreholes is modelled.

6. Effect of *in situ* stress state

Three *in situ* stress scenarios are investigated. In the base case, the three principal far-field stresses are assigned their mean expected values at 500 m depth at Forsmark (Table 1). In the two other cases, the stress magnitudes are chosen such that, within their given uncertainty range, stress anisotropy is minimised and maximised, respectively. Previous studies have shown that fracture growth is promoted by higher stress differentials at the borehole wall.^{76,77} Thus, the other two limiting stress scenarios correspond to (1) $\sigma_H - \sigma_h = \max.$, $\sigma_H - \sigma_v = \max.$ and (2) $\sigma_H - \sigma_h = \min.$, $\sigma_H - \sigma_v = \min.$

Fig. 11A shows the maximum principal stress at the borehole wall, σ_1 , corresponding to the base case and the two limiting *in situ* stress conditions at Forsmark. Assuming there are no fluids in the borehole, the tangential stress varies in a sinusoidal fashion as a function of angular distance, matching the analytical solution for a circular hole in a rock mass with prescribed far-field principal stresses.⁴² In all cases, the tangential stress exceeds $(0.4 \pm 0.05) \times \text{UCS}$, or the “spalling strength” the rock mass.¹⁵ Fig. 11 B and C show how several far-field stress scenarios within the limiting cases affect the borehole tangential stress magnitude. For constant far-field maximum stress σ_H , variations of the far-field intermediate stress σ_h cause borehole Hoop stress changes within $\pm 5\%$ of its base case value at 90° and 270°, where spalling is expected to occur. Conversely, varying the far-field maximum stress σ_H causes large σ_1 changes at the borehole wall, covering the entire range between the two limiting stress scenarios. This shows that variations of the intermediate *in situ* stress alone are less important in affecting the maximum borehole stress at Forsmark than variations of the maximum *in situ* stress. Since all cases fall within the minimum (red line) and maximum (blue line) stress differential scenarios, subsequent fracturing behaviour was analysed in these limiting cases.

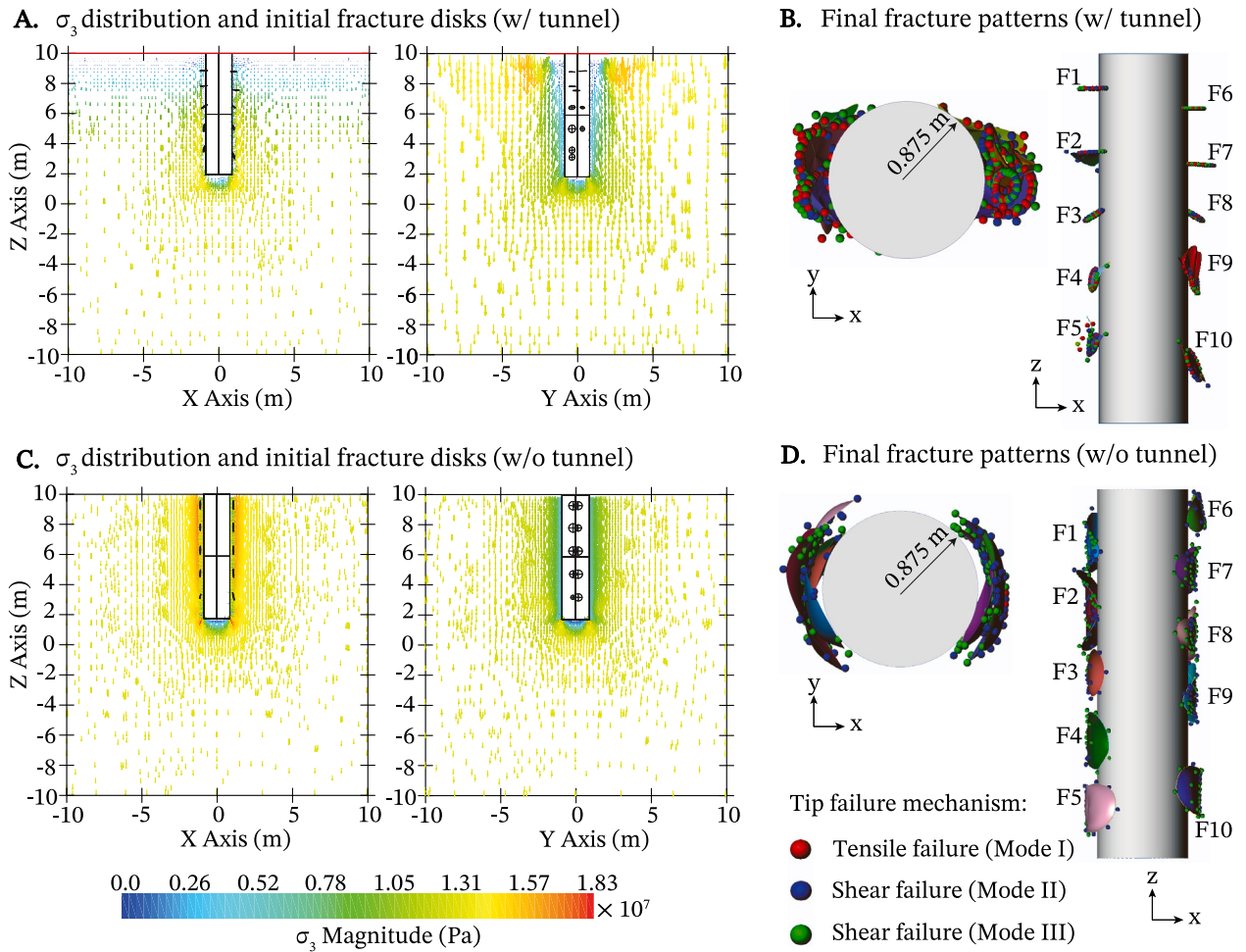
Fig. 10A shows the simulated fracture patterns for the three different stress magnitude scenarios (Fig. 11 A), at perpendicular orientation of the deposition tunnel relative to the direction of the maximum compressive stress. Fractures nucleate and grow diametrically opposite around the circular borehole in the direction of the least compressive horizontal stress, as observed in numerous occasions in the field.^{72,78,79} Fracture growth is influenced by the relative magnitudes of the stresses applied, with more damage and growth occurring under conditions of higher horizontal stress differential. Fracture growth is quantified in terms of the maximum spalled zone depth, spalled zone angle (width), and total fracture surface area (Table 2).

The maximum depths of the fractured zones are comparable with predictions from Martin et al.’s⁸⁰ empirical equation, which relates the ratio of maximum spalling depth and borehole radius to the ratio of maximum tangential stress at the excavation wall and the uniaxial compressive strength of the rock. Given the *in situ* stresses and borehole geometry at Forsmark, Martin et al.’s⁸⁰ empirical relationship predicts spalling depths ranging from a few centimetres in the case of lowest stress differential, up to around 15 cm in the case of highest stress differential. The simulated numerical results in this paper are in better agreement with Martin’s empirical relationship in the case where the deposition tunnel is not considered (Table 2), and also in the case where the tunnel is considered, for those the fractured zones residing in the lower half of the borehole (Fig. 9). However, fracture nucleation and growth at the top of the borehole is affected by the deposition tunnel, causing a larger apparent maximum spalling depth than the one predicted by Martin¹⁷ for the Forsmark site specifically. Another reason for the additional fracture growth might be that Martin¹⁷ looks exclusively at tensile fractures, whereas in this study, spalling fractures initiate in tension, but macro-failure is multi-modal, and generally dominated by shear in the later growth stages.

Since the depths and widths of breakouts in vertical boreholes depend on the relative magnitudes of the major and minor horizontal *in situ* stresses,¹ fracture patterns and final breakout metrics are shown in Fig. 10 only for the base-case and limiting stress scenarios, assuming that any other stress state that falls between the limiting cases will also cause fracturing severity to fall in the expected range.

7. Effect of the borehole geometry

This section investigates the effect of borehole geometry on the predicted spalling around a typical deposition borehole. The cylindrical borehole is modified at the junction with the tunnel floor to provide an access ramp for the spent fuel canisters. This is a potentially practical modification that could provide additional space to operate the machines that will lower the spent fuel canisters into the borehole,



E. Fracture growth measurements

Fracture	Z [m]	With Tunnel			Z [m]	Without Tunnel		
		Spalling Depth [m]	Spalling Width [°]	Surface Area [m ²]		Spalling Depth [m]	Spalling Width [°]	Surface Area [m ²]
F1	0.5	0.43	60.3	1.13	0.5	0.17	50.3	0.50
F2	1.2	0.44	55.1	1.08	1.1	0.15	50.0	0.47
F3	2.4	0.41	63.6	0.98	2.4	0.14	43.7	0.51
F4	2.3	0.15	77.1	1.10	2.3	0.16	57.1	1.10
F5	5.9	0.17	69.5	1.16	5.2	0.14	39.5	1.07
F6	0.4	0.42	40.3	1.12	0.7	0.20	32.0	0.49
F7	0.8	0.40	62.0	0.93	0.8	0.18	52.0	0.55
F8	4.8	0.39	41.2	0.88	4.8	0.18	32.3	0.62
F9	4.6	0.20	54.9	0.95	4.6	0.19	32.9	1.09
F10	6.5	0.23	54.1	1.09	6.5	0.20	44.7	1.05

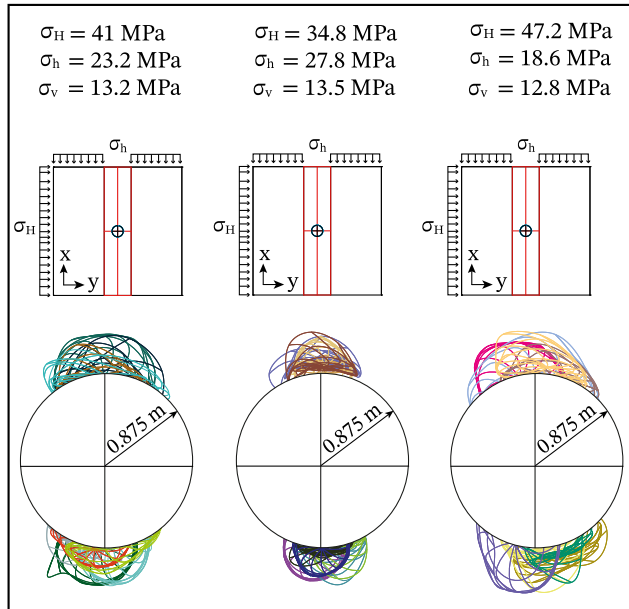
Fig. 9. Effect of the deposition tunnel on fracture growth. Minimum principal stress distribution and initial fracture disks orientations in the 20 m × 20 m × 20 m borehole model, with a free top surface acting as the deposition tunnel floor (A) and corresponding final fracture surfaces (B); 20 m × 20 m × 20 m simple borehole model, where the tunnel effect was not considered (C) and corresponding final fracture patterns (D). Fracture tip nodes are represented as spheres, and coloured according to their dominant failure mechanism. Maximum spalling depth, width and surface area are listed for each macro-fracture that interacts with the borehole wall (E). Z is the distance from the tunnel floor/top domain boundary to the barycentre of the respective fracture. Simulation parameters and boundary conditions are those from Forsmark. (Table 1). (For interpretation of the references to colour in this figure legend, the reader is referred to the web version of this article.)

effectively reducing the requirement of a ‘tall’ deposition tunnel.⁶⁸ Three cases were investigated, where the borehole top is cylindrical, conical, and wedge-shaped (Fig. 5D).

Numerical results show that spalling occurs in all cases, and the modified geometries affect fracture nucleation and growth patterns (Fig. 12). Fractures nucleate uniformly on either side of the circular borehole (Fig. 12A), but concentrate at the borehole top in the other

two simulated cases (Fig. 12B, C). The enlargement of the borehole top induces higher stress concentrations at the borehole–tunnel junction, increasing the depth of failure in the top metre of the borehole. Fracture growth is quantified in terms of maximum spalling depths and total fractured surface areas, showing increasing severity from circular, to cone to wedge (Table 2). Although the maximum absolute spalling depth is the largest in the wedge-shaped borehole case, this

A. Effect of *in situ* stress state



B. Effect of tunnel orientation

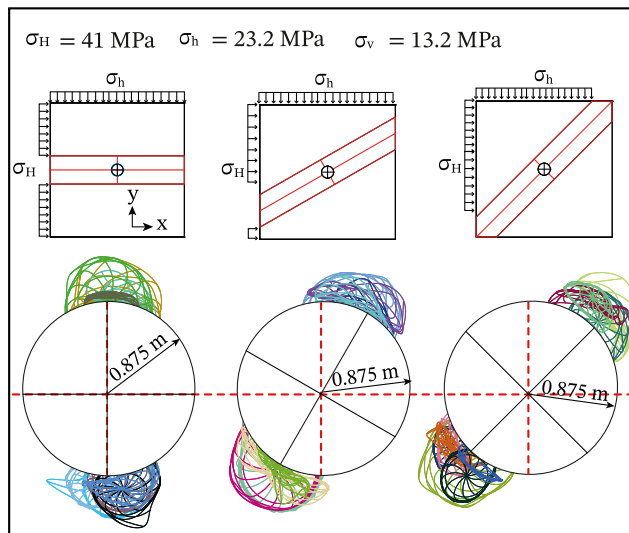


Fig. 10. Effect of *in situ* stress magnitude (A) and tunnel orientation (B) on simulated fracture patterns, showing final fracture geometries after growth has arrested. Tunnel floor is shown in red. Fracture growth in each case is quantified in Table 1. Simulation parameters and boundary conditions are those from Forsmark (Table 1).

only occurs when the tunnel floor is orientated perpendicular to the maximum compressive stress direction. The fractured areas responsible for increased spalling depth on the ramp side are, as shown in Fig. 12C, involved in the spalling of the ramp itself. However, if the tunnel is orientated parallel to the direction of the maximum compressive stress, the stress conditions required for ramp spalling would not be fulfilled and fractures would nucleate 90° away from the ramp, and grow as in the case of a simple circular borehole. For this reason, the symmetrical cone top is not recommended, as it would increase failure severity in all stress and/or tunnel orientation scenarios.

8. Effect of nearby boreholes

In all cases presented previously, fracturing processes were localised in the near-borehole region, and the maximum depth of the fractured

Table 2

Effect of different repository configurations on the maximum fractured depth, width and surface area. Simulation parameters and boundary conditions are those from Forsmark (Table 1).

Case	Maximum spalling depth [m]	Spalling width [°]	Total fractured surface area [m ²]
Deposition tunnel			
w/ tunnel	0.44	63/52	2.0
w/o tunnel	0.19	58/51	13.2
Tunnel orientation			
0°	0.44	63/52	2.0
15°	0.48	23/65	2.34
30°	0.42	90/50	2.60
45°	0.48	27/27	2.11
90°	0.45	52/32	2.28
In situ stress			
Base case	0.44	63/52	2.0
max σ ani.	0.53	58/51	1.63
min σ ani.	0.36	62/55	2.24
Borehole configuration			
Circular	0.44	63/52	2.0
Cone	0.47	60/62	2.23
Wedge	0.49	90/47	2.46
Borehole spacing (3 boreholes)			
3 m	Overlap	135/155	120.5
4 m	0.51	68/96	68.7
5 m	0.46	60/63	52.5
6 m	0.44	63/58	35.3
6 m, ramp	0.49	90/47	26.4
Number of boreholes (6 m spacing)			
3	0.44	60/120	35.3
5	0.48	63/109	67.9
7	0.45	62/117	92.3

zones, although variable from case to case, did not exceed one borehole radius. In this section, additional boreholes are added, as shown in Fig. 5C, at an equal inter-borehole distance, on either side of the central circular borehole. The inter-borehole distance is then gradually decreased, to examine the changes in the displacement and stress fields, and their effects on fracture growth.

8.1. Effect of borehole spacing

The inter-borehole spacing is an important parameter affecting repository design, since it determines the minimum length of the deposition tunnel necessary to accommodate the required number of deposition boreholes. Ideally, this distance would be small enough that as many deposition boreholes as possible would fit along the same deposition tunnel, but large enough to preserve the structural integrity of the repository. The current site-descriptive model from Forsmark recommends a distance of either 6 m (in rock domain RFM029) or 6.8 m (in RFM045) between adjacent boreholes, in the case where thermal effects are also considered.³¹ In this study, the inter-borehole distance, s , is measured between borehole centres, hence the effective pillar width is given by $s - 2R$, where R is the borehole radius.

Fig. 13 shows the normalised maximum principal stress at the borehole wall that arises when two boreholes are subjected to transverse, longitudinal and biaxial tension respectively. For validation purposes, the maximum circumferential stress was plotted against the closed-form solutions derived for these cases by Ling,⁸¹ showing a good match.

Fig. 14 shows the elastic modulus distribution in the meshed rock domain, alongside corresponding fracture patterns in a three-borehole model. The boreholes are evenly spaced, at $s = 2.5$ (A), 3.0 (B), 3.5 (C) and 4.0 (D) metres apart, resulting in the formation of two over-stressed pillars of equal widths, $P_w = 0.75$ (A), 1.25 (B), 1.75 (C) and 2.25

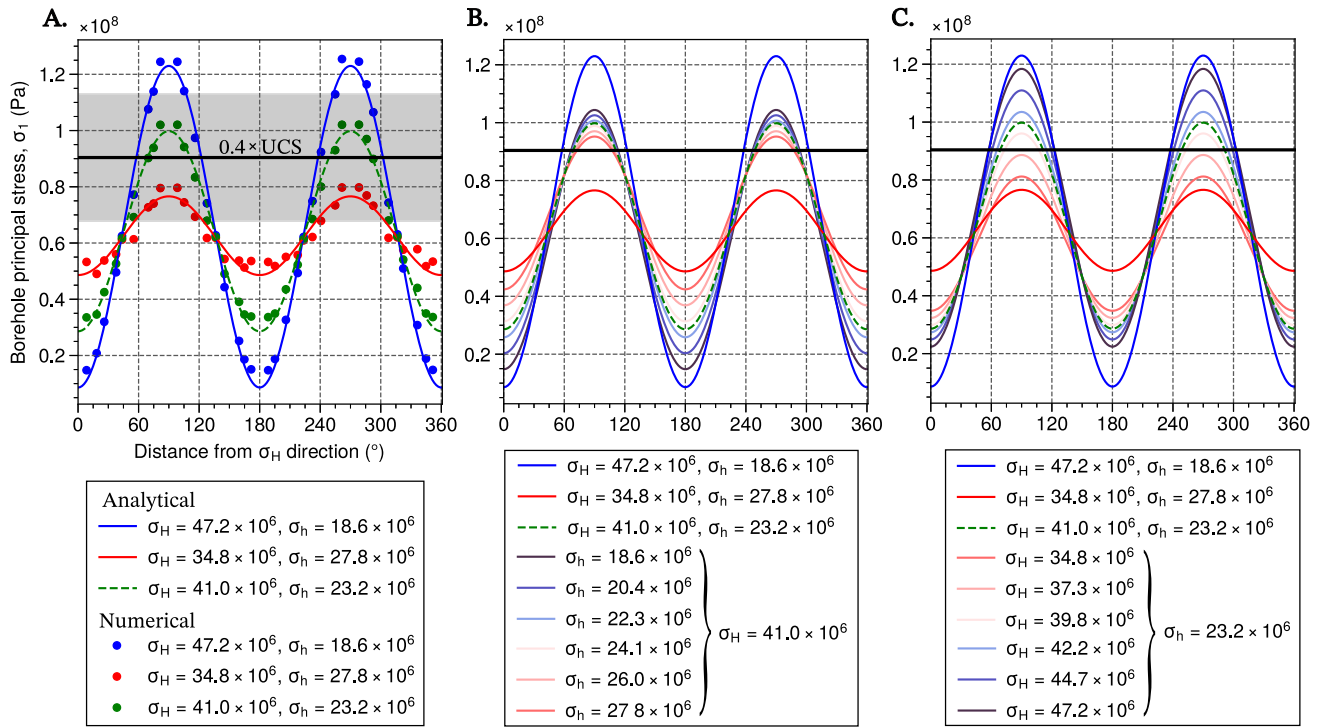


Fig. 11. Effect of *in situ* stress magnitudes at Forsmark on maximum principal stress at the borehole wall (tangential stress), prior to fracture nucleation (A). Effect of varying σ_h , while keeping σ_H constant (B) and vice versa (C). The horizontal line at 90.4 MPa, corresponds to 0.4 $\times$ UCS, and the grey shaded area shows the range of stresses favourable to spalling initiation, spanning between 0.3 $\times$ UCS and 0.5 $\times$ UCS. (For interpretation of the references to colour in this figure legend, the reader is referred to the web version of this article.)

(D), respectively. For a borehole spacing of 2.5 m, the two pillars are completely damaged, as shown by the reduction in Young's Modulus. As the inter-borehole distance increases, the damage and corresponding reduction in Young's modulus decreases and becomes more localised to the walls of the boreholes. In the case of a 4.0 m spacing (Fig. 14D), the centre of each pillar shows a 15% reduction of the original, undamaged Young's modulus, and areas of high damage are localised in the vicinity of the borehole wall, where the fractured regions reside. In this case, the three boreholes are not interconnected by a fractured zone and the region between them is largely undamaged.

In all cases, Young's Modulus remains unchanged from about one metre away from the peripheral boreholes, implying that beyond this point the host rock is undisturbed. Table 2 presents the quantified interaction of the three boreholes spaced at 3, 4, 5, and 6 m, noting the maximum observed failure depth, width, and the total fractured surface area in each case.

8.2. Effect of total number of boreholes

Fig. 15 shows maximum, intermediate and minimum principal stress distributions around a three- (A) and a five-borehole (B) array, spaced 4 m and 6 m apart, respectively. The three/five aligned boreholes are created simultaneously, and their locations are varied in order to investigate the effect of borehole spacing, as well as the impact of total number of boreholes within an array, on borehole and pillar stress. The applied horizontal stresses are in all cases are $\sigma_H = 41$ MPa and $\sigma_h = 23.2$ MPa, the vertical stress is $\sigma_v = 13.2$ MPa. Fig. 15C shows the maximum principal stress magnitude variation along the two pillars, in four different pillar widths scenarios. Comparison of the resulting stress magnitudes in the two pillars shows a 31% pillar stress increase when the inter-borehole distance decreases from 4.0 m to 2.5 m. While borehole spacing is shown to influence stress concentrations in the pillars (C), the number of boreholes in the model (D) does not influence stress distribution.

Fig. 15D shows the maximum principal stress magnitude lateral variation in a three-, five- and seven-borehole model, respectively. In all three simulation models, the borehole spacing is six metres, and the domain was adjusted such as the distance between the outermost boreholes and the domain boundaries remains constant, in order to avoid boundary effects. The purpose of this exercise was to investigate whether increasing the number of neighbouring boreholes has an effect on stress state and fracture patterns around the innermost central borehole(s). It is apparent that regardless of the number of simulated boreholes, provided that the inter-borehole distance remains unchanged and sufficiently large, and the numerical domain is adjusted accordingly, there is no major change in the stress state around the central and outer boreholes.

This determines all boreholes to be equally damaged and hence fracture growth amounts to similar fractured area per borehole as well (Table 2). This result agrees qualitatively with those obtained by Horii and Nemat-Nasser⁸² and Wang et al.⁸³ for larger numbers of collinear circular openings under uniaxial tension. Their numerical studies also showed that interaction between central and non-central boreholes can be neglected as long as the borehole spacing is large enough to prevent interactions.

This is a useful result, supporting the validity of three-borehole simulation cases extrapolated to the repository scale. The alternative of simulating cm-scale, concurrent fracturing processes around dozens of deposition boreholes per deposition tunnel would not be computationally efficient yet. For example, in the single borehole case, there are initially 31,911 nodes, which, after the nucleation of ten fractures, increase to 75,704 nodes. For three boreholes, the number of nodes increases from 69,068 in the intact rock domain to 173,942 after the nucleation of thirty fractures, and finally, for five boreholes it increases from 106,920 to 282,170 after the nucleation of fifty fractures. The number of nodes keeps increasing as the fractures continue to grow, adding detailed discretisation to regions of the mesh that were previously coarser.

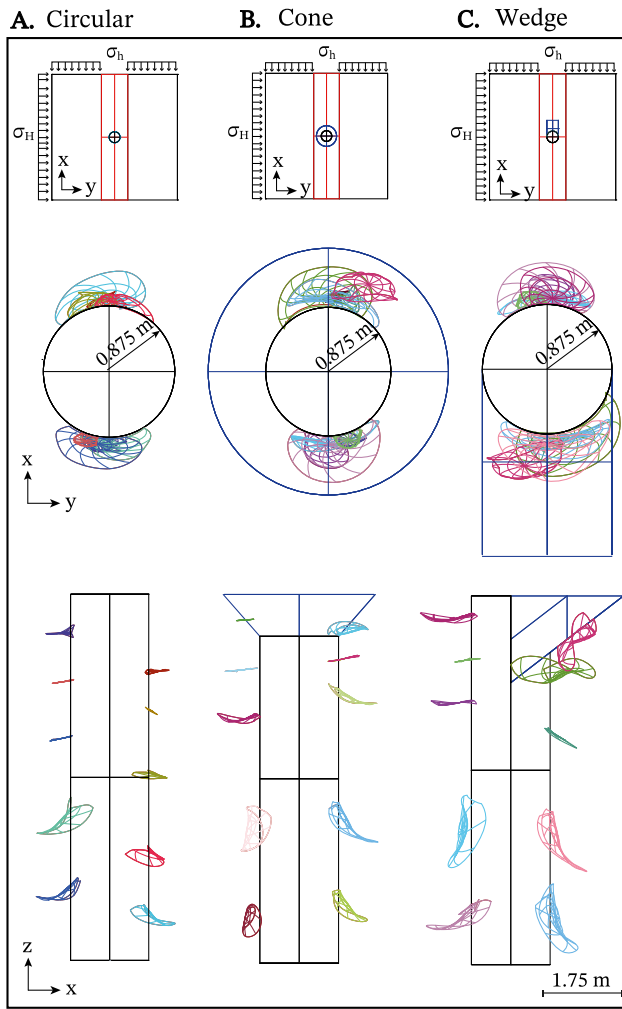


Fig. 12. Effect of borehole geometry on simulated fracture patterns, showing final borehole breakout extent for a circular (A), cone-shaped (B) and wedge-shaped (C) borehole. Fracture growth is quantified in Table 1. Simulation parameters and boundary conditions are those from Forsmark (Table 1).

9. Conclusions

Geometric fracture growth simulations, based on the finite element method, are used to investigate stress-induced fracturing processes leading to spalling around deposition boreholes under different underground conditions. Several scenarios are considered, in which the *in situ* stresses, borehole geometries and spacing are varied to assess the impact on final fracture patterns and damage extent.

In the case of a single circular borehole excavated in a homogeneous and isotropic rock domain, results indicate that spalling failure initiates in areas of high damage close to the borehole wall and continues to develop in the direction of the least compressive stress σ_3 , in agreement with experimental observations from laboratory and field studies.

When the deposition tunnel floor was incorporated as a free surface, fractures at the intersection between the borehole and the tunnel nucleated preferentially parallel to the tunnel floor, and perpendicular to the borehole wall. This results in larger apparent spalled zones widths, but lower fractured surface area. Fractures in that case grow more horizontally, in the direction of the tunnel floor, instead of vertically, along the borehole itself. They also show less interaction with the borehole wall in the borehole axial direction. The tunnel is then rotated at different angles to the maximum horizontal stress, in

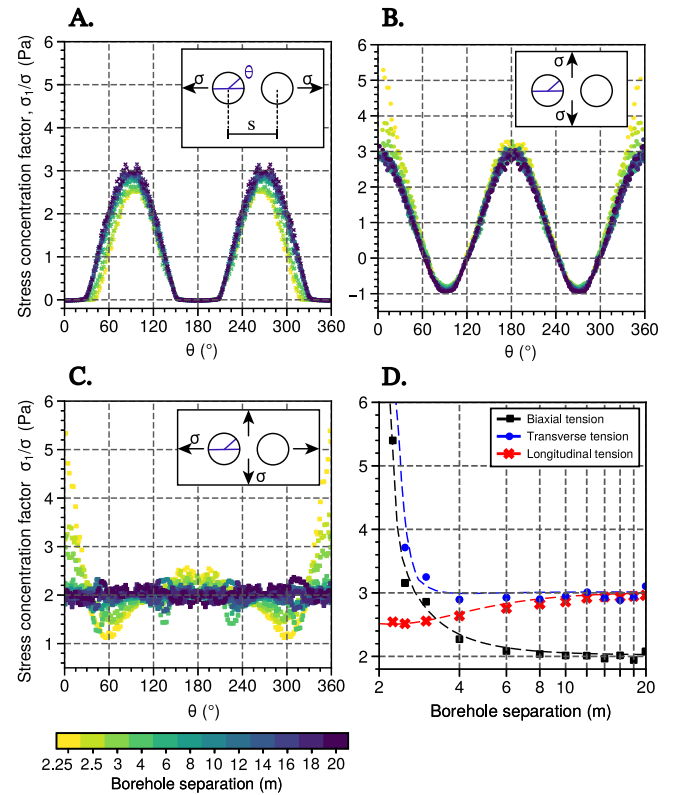


Fig. 13. Tangential stress at the borehole wall, normalised to the applied load, under longitudinal (A), transverse (B) and biaxial (C) tensile loading. Simulation parameters are those from Forsmark (Table 1). Effect of borehole spacing on the maximum principal stress at the borehole wall, in a two-borehole model, under uniaxial and biaxial loading (D). The maximum tangential stress occurs at $\theta = 0^\circ$ for uniaxial transverse and biaxial tension, and at $\theta = \pm 90^\circ$ for longitudinal loading, in agreement with Ling's⁸¹ analytical solution.

order to assess the impact of stress orientation on fracture growth. Since the relative stress magnitudes were kept constant regardless of the tunnel orientation, the extent of the fractured zone remained consistent in all orientation cases, for a circular borehole.

Another scenario that was examined was the influence of varying *in situ* stress magnitudes. Results showed that damage exceeds the threshold required for nucleation in the near-borehole region, for the expected subsurface conditions at Forsmark. In the case of higher far-field stress differential, the fractures involved in spalling extended deeper into the host rock domain, while lower stress anisotropy caused the fracturing process to stabilise more locally.

Finally, the effect of neighbouring boreholes and inter-borehole distance was investigated. Numerical results determined that the maximum stress concentration around flanked boreholes does not change significantly, regardless of the total number of boreholes in the model, provided that the domain boundaries and number of initial disks are adjusted accordingly, and the inter-borehole distance remains constant. The three-, five- and seven-borehole models were fundamentally identical in terms of fracturing behaviour around flanked (inner) borehole(s), and flanking (outer) boreholes. On the other hand, inter-borehole spacing plays an important role in pillar stability. Under the given conditions at Forsmark, circular boreholes need to be spaced at least 4 m apart to maintain pillar integrity under mechanical loading. In the case where access ramps are drilled, the spacing should be increased to at least 6 m, so that stability of the top of the borehole is ensured.

The present approach to model spalling failure and general fracturing around boreholes provides useful and quick information for designing a safe repository for nuclear waste storage. The modelled

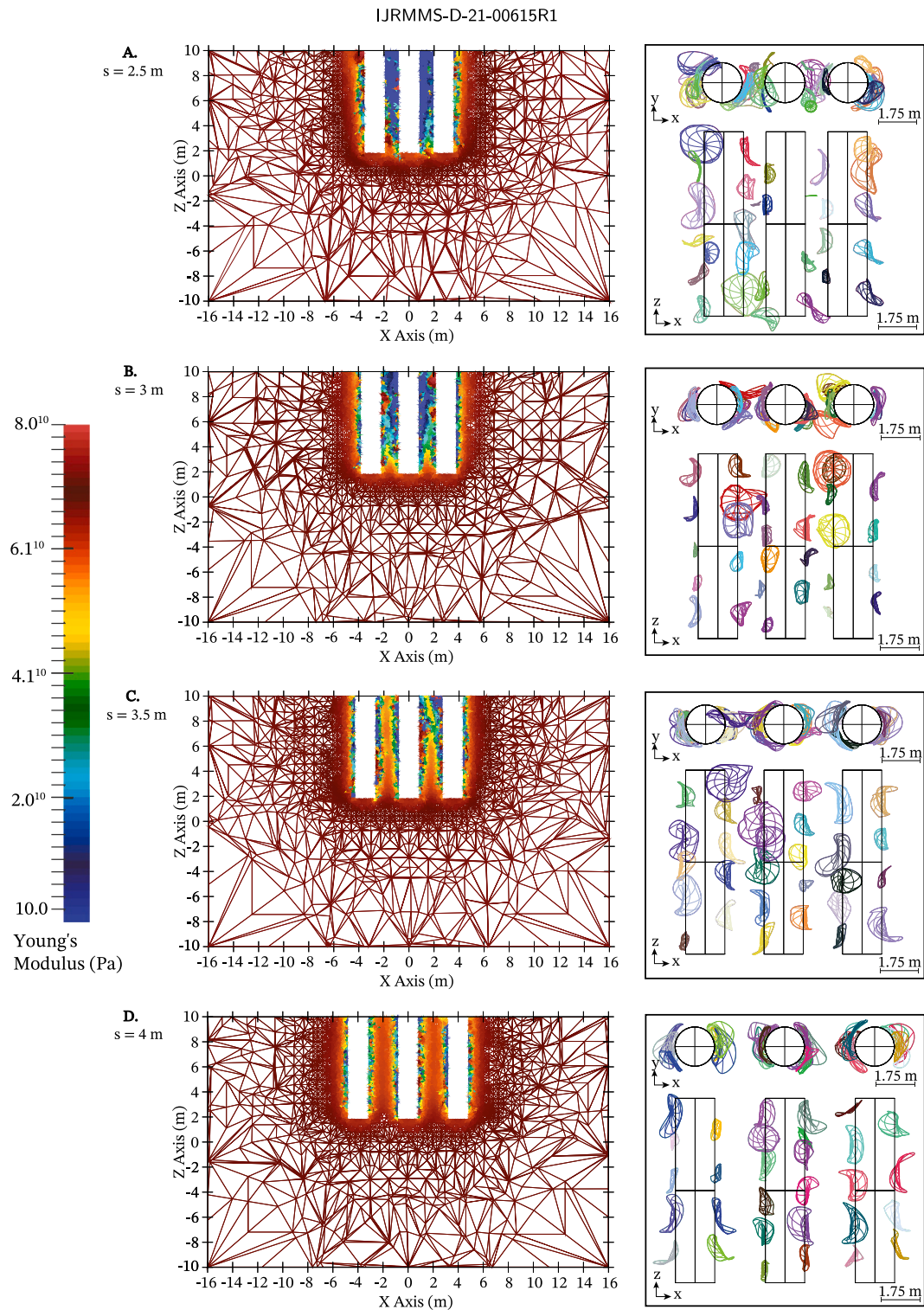


Fig. 14. Mesh cut planes showing Young's Modulus for inter-borehole distances of 2.5 (A), 3.0 (B), 3.5 (C) and 4.0 (D) m, and corresponding fracture geometries. For closer spaced boreholes, the decrease in Young's Modulus due to increased damage is substantial, and correlates to the increase in the fractured area. Simulation parameters and boundary conditions are those from Forsmark (Table 1).

fracture patterns are consistent with previous studies and field observations, and the computational cost to generate them is low. The extent of the fractured areas around boreholes depends on a number of repository design parameters, some of which can be adjusted to minimise spalling severity and eventually reduce the risk for leaking radionuclides in this highly fractured zone.

CRediT authorship contribution statement

M. Cristina Saceanu: Conceptualisation, Methodology, Software, Validation, Visualisation, Writing - Original Draft. **Adriana Paluszny:** Methodology, Software, Supervision, Writing - Review & Editing. **Robert W. Zimmerman:** Methodology, Supervision, Writing - Review & Editing. **Diego Mas Ivars:** Supervision, Writing - Review & Editing.

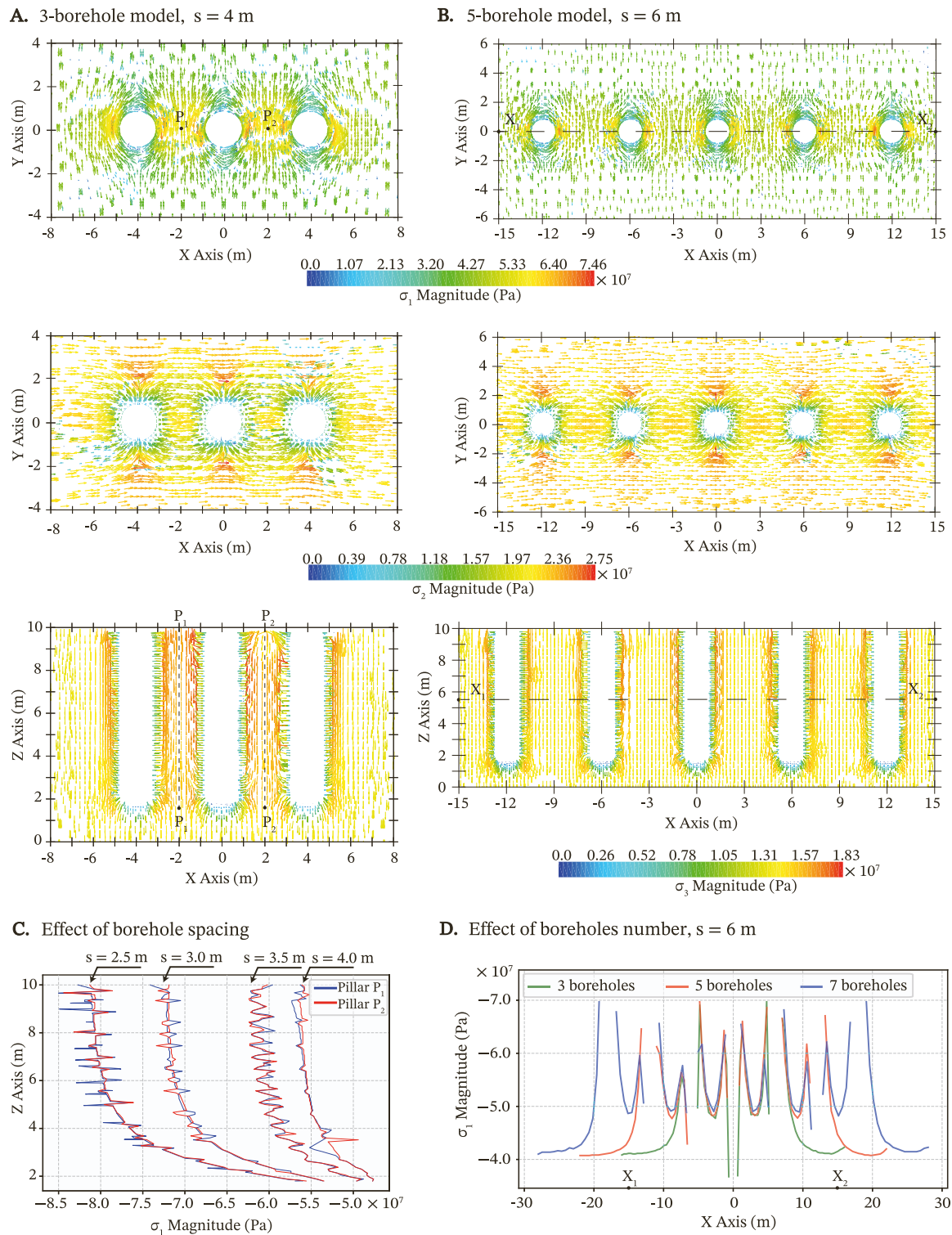


Fig. 15. Principal stresses magnitudes and orientations around circular boreholes spaced 4 m (A) and 6 m (B) apart, in a 3- and 5-borehole model respectively. σ_1 magnitude at the centre of the two pillars P_1 and P_2 is plotted for the different borehole spacing cases (C). σ_1 magnitude is also plotted over a line X_1X_2 traversing the 5-borehole model laterally, at a depth 4.5 m down the borehole (D). Overimposed 3- and 7-borehole models show similar variations of σ_1 . Simulation parameters and boundary conditions are those from Forsmark (Table 1).

Declaration of competing interest

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

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Appendix A. Supplementary data

Supplementary material related to this article can be found online at <https://doi.org/10.1016/j.ijrmms.2022.105038>.

References

- Amadei B, Stephansson O. Borehole breakout method. In: *Rock stress and its measurement*. London: Chapman & Hall; 1997:303–325.
- Fairhurst C, Cook N. The phenomenon of rock splitting parallel to the direction of maximum compression in the neighbourhood of a surface. In: *1st ISRM congress*. Lisbon, Portugal: 1966:687–692. Paper ISRM-1CONGRESS-1966-115.
- Ewy R, Cook N. Deformation and fracture around cylindrical openings in rock—I. Observations and analysis of deformations. *Int J Rock Mech Mining Sci Geomech Abstr*. 1990;27(5):387–407.
- Peška P, Zoback MD. Compressive and tensile failure of inclined well bores and determination of in situ stress and rock strength. *J Geophys Res Solid Earth*. 1995;100(B7):12791–12811.
- Zheng Z, Kemeny J, Cook NGW. Analysis of borehole breakouts. *J Geophys Res*. 1989;94(B6):7171.
- Read R. 20 Years of excavation response studies at AECL's Underground Research Laboratory. *Int J Rock Mech Mining Sci*. 2004;41(8):1251–1275.
- Martin CD, Kaiser PK. *Mine-by experiment committee report phase 1: Excavation response summary and implications*. Technical Report AECL-11382; Atomic Energy Canada Limited; 1996.
- Andersson JC. *Äspö pillar stability experiment: rock mass response to coupled mechanical thermal loading* (Ph.D. thesis). KTH, Stockholm; 2007.
- Santarelli F, Brown E. Failure of three sedimentary rocks in triaxial and hollow cylinder compression tests. *Int J Rock Mech Mining Sci Geomech Abstr*. 1989;26(5):401–413.
- Haimson B, Song I. Laboratory study of borehole breakouts in cordova cream: a case of shear failure mechanism. *Int J Rock Mech Mining Sci Geomech Abstr*. 1993;30(7):1047–1056.
- Shen B, Barton N. Rock fracturing mechanisms around underground openings. *Geomech Eng*. 2018;16:35–47.
- Haimson B. Micromechanisms of borehole instability leading to breakouts in rocks. *Int J Rock Mech Mining Sci*. 2007;44(2):157–173.
- Hajjabbolmajid V, Kaiser P, Martin C. Modelling brittle failure of rock. *Int J Rock Mech Mining Sci*. 2002;39(6):731–741.
- Mobilised strength components in brittle failure of rock. *Geotechnique*. 2003;53:327–336.
- Diederichs MS. The 2003 Canadian geotechnical colloquium: Mechanistic interpretation and practical application of damage and spalling prediction criteria for deep tunnelling. *Can Geotech J*. 2007;44(9):1082–1116.
- Diederichs MS, Kaiser PK, Eberhardt E. Damage initiation and propagation in hard rock during tunnelling and the influence of near-face stress rotation. *Int J Rock Mech Mining Sci*. 2004;41(5):785–812.
- Martin D. The impact of brittle behaviour of rocks on tunnel excavation design. In: *International society for rock mechanics and rock engineering*. 2004.
- Cundall PA, Hart RD. Numerical modeling of discontinua. In: *Comprehensive rock engineering : principles, practice & projects*. Pergamon; 1993:231–243.
- Cundall PA. A discontinuous future for numerical modelling in geomechanics? *Proc Inst Civil Eng Geotechn Eng*. 2001;149(1):41–47.
- Lan H, Martin CD, Hu B. Effect of heterogeneity of brittle rock on micromechanical extensile behavior during compression loading. *J Geophys Res*. 2010;115(B1).
- A dem model for soft and hard rocks: role of grain interlocking on strength. *Journal of the Mechanics and Physics of Solids*. 2013;61:352–369.
- Potyondy DO, Cundall PA. A bonded-particle model for rock. *International Journal of Rock Mechanics and Mining Sciences*. 2004;41:1329–1364.
- Lan H, Martin CD, Andersson JC. Evolution of in situ rock mass damage induced by mechanical–thermal loading. *Rock Mech Rock Eng*. 2013;46(1):153–168.
- Elmo D, Stead D. An integrated numerical modelling–discrete fracture network approach applied to the characterisation of rock mass strength of naturally fractured pillars. *Rock Mech Rock Eng*. 2009;43(1):3–19.
- Vlachopoulos N, Vazaios I. The numerical simulation of hard rocks for tunnelling purposes at great depths: a comparison between the hybrid fdem method and continuous techniques. *Advances in Civil Engineering*. 2018;2018.
- Cai M, Kaiser PK, Tasaka Y, Maejima T, Morioka H, Minami M. Generalized crack initiation and crack damage stress thresholds of brittle rock masses near underground excavations. *Int J Rock Mech Mining Sci*. 2004;41(5):833–847.
- Mitelman A, Elmo D. Modelling of blast-induced damage in tunnels using a hybrid finite-discrete numerical approach. *J Rock Mech Geotechn Eng*. 2014;6(6):565–573.
- Hamdi P, Stead D, Elmo D. Damage characterization during laboratory strength testing: A 3D-finite-discrete element approach. *Comput Geotech*. 2014;60:33–46.
- Lisjak A, Grasselli G. A review of discrete modeling techniques for fracturing processes in discontinuous rock masses. *J Rock Mech Geotech Eng*. 2014;6(4):301–314.
- Bodansky D. *Nuclear energy: Principles, practices, and prospects*. American Institute of Physics; 1996.
- SKB TR-08-05. *Site description of Forsmark at completion of the site investigation phase. SDM-site Forsmark*. Technical Report; Svensk Kärnbränslehantering AB; 2007.
- SKB TR-08-05. *Rock mechanics Forsmark: Site descriptive modelling Forsmark stage 2.2*. Technical Report; Svensk Kärnbränslehantering AB; 2007.
- SKB R-07-26. *Quantifying in situ stress magnitudes and orientations for Forsmark, Forsmark stage 2.2*. Technical Report; Svensk Kärnbränslehantering AB; 2007.
- SKB P-07-166. *Forsmark site investigation, mapping of borehole breakouts, processing of acoustical televiewer data from KFM08A, KFM08C, KFM09A, KFM09A, KFM09B and KFM09B*. Technical Report; Svensk Kärnbränslehantering AB; 2007.
- SKB R-08-116. *Underground design Forsmark layout D2*. Technical Report; Svensk Kärnbränslehantering AB; 2009.
- Thomas RN, Paluszny A, Zimmerman RW. Quantification of fracture interaction using stress intensity factor variation maps. *J Geophys Res Solid Earth*. 2017;122(10):7698–7717.
- Salimzadeh S, Paluszny A, Zimmerman RW. Three-dimensional poroelastic effects during hydraulic fracturing in permeable rocks. *Int J Solids Struct*. 2017;108:153–163.
- Salimzadeh S, Usui T, Paluszny A, Zimmerman RW. Finite element simulations of interactions between multiple hydraulic fractures in a poroelastic rock. *Int J Rock Mech Mining Sci*. 2017;99:9–20.
- Salimzadeh S, Paluszny A, Nick HM, Zimmerman RW. A three-dimensional coupled thermo-hydro-mechanical model for deformable fractured geothermal systems. *Geothermics*. 2018;71:212–224.
- Paluszny A, Zimmerman RW. Numerical fracture growth modeling using smooth surface geometric deformation. *Eng Fract Mech*. 2013;108:19–36.
- Cook RD, Malkus DS, Plesha ME. *Concepts and applications of finite element analysis*. John Wiley & Sons; 2001.
- Jaeger JC, Cook NGW, Zimmerman R. *Fundamentals of rock mechanics*. John Wiley and Sons Ltd; 2007.
- Nejati M, Paluszny A, Zimmerman RW. A finite element framework for modeling internal frictional contact in three-dimensional fractured media using unstructured tetrahedral meshes. *Comput Methods Appl Mech Eng*. 2016;306:123–150.
- Barton N, Bandis S. Review of predictive capabilities of JRC-JCS model in engineering practice. In: *Rock joints, proc. int. symp. on rock joints*. 1990:603–610.
- Mazars J. *Application of continuous damage mechanic to non-linear behavior of concrete structures (in French)* (Ph.D. thesis). Paris 6 University; 1984.
- Lemaître J. How to use damage mechanics. *Nuclear Eng Des*. 1984;80(2):233–245. 4th Special Issue on Smirt-7.
- SKB P-05-211. *Forsmark site investigation Borehole KFM07A Uniaxial compression test of intact rock*. Technical Report; Svensk Kärnbränslehantering AB; 2005.
- SKB P-05-212. *Forsmark site investigation Drill hole KFM07A Indirect tensile strength test including strain measurement*. Technical Report; Svensk Kärnbränslehantering AB; 2006.
- Souissi S, Miled K, Hamdi E, Hedi S. Numerical modelling of rocks damage during indentation process with reference to hard rock drilling. *Int J Geomech (ASCE)*. 2017;17.
- Irwin GR. Analysis of stresses and strains near the end of a crack traversing a plate. *J Appl Mech*. 1957;24:361–364.
- Paluszny A, Zimmerman RW. Numerical simulation of multiple 3D fracture propagation using arbitrary meshes. *Comput Methods Appl Mech Eng*. 2011;200(9–12):953–966.
- Nejati M, Paluszny A, Zimmerman RW. A disk-shaped domain integral method for the computation of stress intensity factors using tetrahedral meshes. *Int J Solids Struct*. 2015;69–70:230–251.
- Nakamura T, Parks D. Antisymmetrical 3-D stress field near the crack front of a thin elastic plate. *Int J Solids Struct*. 1989;25(12):1411–1426.
- Henshell RD, Shaw KG. Crack tip finite elements are unnecessary. *Int J Numer Methods Eng*. 1975;9(3):495–507.
- Nejati M, Paluszny A, Zimmerman RW. On the use of quarter-point tetrahedral finite elements in linear elastic fracture mechanics. *Eng Fract Mech*. 2015;144:194–221.
- Richard HA, Fulland M, Sander M. Theoretical crack path prediction. *Fatigue Fracture Eng Mater Struct*. 2005;28(1–2):3–12.
- Chang S-H, Lee C-I, Jeon S. Measurement of rock fracture toughness under modes I and II and mixed-mode conditions by using disc-type specimens. *Eng Geol*. 2002;66(1–2):79–97.
- Aliha M, Bahmani A, Akhondi S. Determination of mode III fracture toughness for different materials using a new designed test configuration. *Mater Des*. 2015;86:863–871.
- Thomas RN, Paluszny A, Zimmerman RW. Growth of three-dimensional fractures, arrays, and networks in brittle rocks under tension and compression. *Computers and Geotechnics*. 2020;121:103447.
- Richard H, Schramm B, Schirmeisen N-H. Cracks on mixed mode loading – theories, experiments, simulations. *Int J Fatigue*. 2014;62:93–103.
- Martha LF, Wawrzyniak PA, Ingraffea AR. Arbitrary crack representation using solid modeling. *Eng Comput*. 1993;9(2):63–82.
- Piegl L, Tiller W. *The NURBS book*. Springer Berlin Heidelberg; 1997.
- Paluszny A, Matthai SK, Hohmeyer M. Hybrid finite element-finite volume discretization of complex geologic structures and a new simulation workflow demonstrated on fractured rocks. *Geofluids*. 2007;7(2):186–208.

64. Koch KR. Three-dimensional NURBS surface estimated by lofting method. *Int J Adv Manufact Technol*. 2009;49(9–12):1059–1068.
65. Paluszny A, Thomas RN, Saceanu MC, Zimmerman RW. Hydro-mechanical interaction effects and channelling in three-dimensional fracture networks undergoing growth and nucleation. *J Rock Mech Geotech Eng*. 2020;12(4):707–719.
66. SKB TR-06-09. *Long-term safety for KBS-3 repositories at Forsmark and Laxemar – a first evaluation*. Technical Report; Svensk Kärnbränslehantering AB; 2006.
67. Saceanu M, Paluszny A, Zimmerman R, Mas Ivars D. Numerical modelling of spalling around a nuclear waste storage deposition borehole using a fracture mechanics approach. In: *54th U.S. rock mechanics/geomechanics symposium*. 2020 Paper ARMA 2020-1396.
68. Pusch R. Repository concepts for HLW including spent fuel and waste with long-lived radionuclides. In: *Geological storage of highly radioactive waste: current concepts and plans for radioactive waste disposal*. Berlin, Heidelberg: Springer Berlin Heidelberg; 2008:167–273.
69. Paluszny A, Matthäi SK. Numerical modeling of discrete multi-crack growth applied to pattern formation in geological brittle media. *Int J Solids Struct*. 2009;46(18–19):3383–3397.
70. Lang PS, Paluszny A, Zimmerman RW. Permeability tensor of three-dimensional fractured porous rock and a comparison to trace map predictions. *J Geophys Res Solid Earth*. 2014;119(8):6288–6307.
71. Zhao X, Cai M, Cai M. Considerations of rock dilation on modeling failure and deformation of hard rocks—a case study of the mine-by test tunnel in Canada. *J Rock Mech Geotechn Eng*. 2010;2(4):338–349.
72. Read RS, Martin CD. *Technical summary of AECL's mine-by experiment: Phase 1: excavation response, number 95–171 in COG*. Pinawa, Manitoba: Atomic Energy of Canada Limited, Whiteshell Laboratories; 1996.
73. Rutqvist J, Digby P, Stephansson O, Singh U. Simulation of borehole breakouts with a damage material model. In: *ISRM international symposium*. 1989 ISRM-IS-1989-172.
74. Hoek E, Brown ET. *Underground excavations in rock*. CRC Press; 1980.
75. SKB TR-02-26. *Boring of full scale deposition holes at the Äspö Hard Rock Laboratory, Operational experiences including boring performance and a work time analysis*. Technical Report; Svensk Kärnbränslehantering AB; 2002.
76. Shen B, Stephansson O, Rinne M. Simulation of borehole breakouts using Fracod2D. *Oil & Gas Sci Technol*. 2002;57(5):579–590.
77. Lisjak A, Figi D, Grasselli G. Fracture development around deep underground excavations: Insights from FDEM modelling. *J Rock Mech Geotechn Eng*. 2014;6(6):493–505.
78. Zoback MD, Moos D, Mastin L, Anderson RN. Well bore breakouts and in situ stress. *J Geophys Res Solid Earth*. 1985;90(B7):5523–5530.
79. Barton CA, Zoback MD, Burns KL. In-situ stress orientation and magnitude at the Fenton Geothermal Site, New Mexico, determined from wellbore breakouts. *Geophys Res Lett*. 1988;15(5):467–470.
80. Martin CD, Kaiser PK, McCreath DR. Hoek-brown parameters for predicting the depth of brittle failure around tunnels. *Can Geotech J*. 1999;36(1):136–151.
81. Ling C-B. On the stresses in a plate containing two circular holes. *J Appl Phys*. 1948;19(1):77–82.
82. Horii H, Nemat-Nasser S. Compression-induced microcrack growth in brittle solids: Axial splitting and shear failure. *J Geophys Res*. 1985;90(B4):3105.
83. Wang J, Crouch S, Mogilevskaia S. A complex boundary integral for multiple circular holes in an infinite plane. *Eng Anal Boundary Elements*. 2003;27:789–802.
84. Rabczuk T. Computational methods for fracture in brittle and quasi-brittle solids: State-of-the-art review and future perspectives. *ISRN Appl Math*. 2013;2013:1–38.