Numerical study on the effect of confinement on thermal spallation drilling of hard rock

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Abstract. This paper presents a numerical study on the effect of confining pressure (down-to-hole pressure) on the performance of the thermal jet drilling on rock. For this end, a numerical method including a viscoplastic-damage model for rock and an explicit scheme for solving the governing thermo-mechanical problem, which is assumed uncoupled. Moreover, rock heterogeneity, which facilitates the thermal spallation phenomenon, is taken into account. Numerical simulations under axisymmetric conditions demonstrate that the efficiency of thermal spallation based drilling is reduces drastically due to confinement.

1. Introduction

Traditional oil well drilling is based on mechanical breakage by rotary or rotary/percussive drilling methods. However, these methods suffer from severe tool wear due to mechanical contact, especially in hard formations. Therefore, under the pressure of ever-increasing demands for efficiency and economy, non-traditional drilling methods using some non-mechanical agent to assist (or to replace) the mechanical breakage have been searched during the last couple of decades.

One particularly attractive non-mechanical method is to utilize the thermal spallation phenomenon illustrated in figure 1. Accordingly, when a rock surface is exposed to an intensive heating the resulting thermal gradient (VT in figure 1) induces a compressive stress state (σ in figure 1), which leads to crack growth in the rock surface layer. When the cracks reach the critical length, spallation, i.e. ejection of rock chips, occurs. The minimal required temperature for spallation to occur in granitic rocks is about 500-600 °C [1]. This method has been successfully applied in drilling granite rock at the laboratory conditions [1-3]. However, while spalling phenomenon can be applied in thermal drilling at shallow depths, it seems that confining conditions, especially the down-the-hole pressure (pconf in figure 1), impedes rock fracturing thus preventing the ejection of spalls (see figure 1). Under such conditions then, the rate of penetration of drilling expectedly drops drastically.

This paper presents a numerical study on the thermal spallation drilling method under such confining conditions. For this end, a numerical simulation method is developed. Previous numerical studies on thermal spallation drilling are, e.g., by Saksala [4] and Walsh and Lomov [5]. The present model is based on a damage-viscoplastic constitutive model for rock implemented in the finite element method. The rock material is assumed to consist three different minerals, Quartz, Feldspar and Biotite. The underlying thermo-mechanical problem is solved as an uncoupled problem where the only information from the thermal part to the mechanical part is the thermal strain, which leads to the spallation of the rock.
2. Theory of the modelling approach

The modelling approach is briefly described here. First, the constitutive model for rock is explained. Then, the time integration scheme of the uncoupled thermo-mechanical problem is outlined.

2.1. Viscoplastic-damage model for rock

The constitutive description of rock chosen here is the damage-viscoplasticity model originally presented by Saksala [6]. In this model, the stress states leading to viscoplastic yield and isotropic damage are indicated by the Drucker-Prager (DP) yield function with the Modified Rankine (MR) criterion as a tension cut-off. Assuming perfectly viscoplastic behaviour, the yield surfaces, along with the rate-dependent cohesion and tensile strength, are written as

\[ f_{\text{DP}}(\sigma, k_{\text{DP}}) = \sqrt{J_2} + \alpha_{\text{DP}} J_1 - k_{\text{DP}} c(k_{\text{DP}}) \]
\[ f_{\text{MR}}(\sigma, k_{\text{MR}}) = \sum_{i=1}^{3} \left( \sigma_i \right)^2 - c_i(k_{\text{MR}}) \]
\[ c(k_{\text{DP}}) = c_0 + s_{\text{DP}} k_{\text{DP}}^i, \quad c_i(k_{\text{MR}}) = \sigma_{10} + s_{\text{MR}} k_{\text{MR}}^i \]  

where \( J_1 = (\sigma_1 + \sigma_2 + \sigma_3) \) and \( J_2 = 0.5((\sigma_1 - \sigma_2)^2 + (\sigma_1 - \sigma_3)^2 + (\sigma_2 - \sigma_3)^2) \) are the first and the second invariants of the stress tensor \( \sigma, \sigma_i \) is the \( i \)th principal stress, \( \langle \rangle \) are the McAuley brackets, \( \alpha_{\text{DP}} \) and \( k_{\text{DP}} \) are the DP parameters, \( c \) and \( \sigma_i \) are the dynamic cohesion and tensile strength (with their intact values \( c_0 \) and \( \sigma_{10} \)) depending on the rates of the internal variables \( k_{\text{DP}} \) and \( k_{\text{MR}} \), respectively. For calibration purposes, the DP parameters are expressed in terms of the friction angle \( \varphi \)

\[ \alpha_{\text{DP}} = 2\sin \varphi(3-\sin \varphi) \]  

\[ k_{\text{DP}} = 6\cos \varphi(3-\sin \varphi) \]

By this choice, the uniaxial compressive strength can be matched. Moreover, a plastic potential of the same form as \( f_{\text{DP}} \) in (1) but with a dilatation angle \( \psi \) (\( \leq \varphi \)) is also employed to mend the poor prediction of dilatancy of the associated flow rule. The internal variable for the DP part in (1) is defined so that the thermodynamic consistency of the model is retained [7]:

\[ k_{\text{DP}} = -\dot{\lambda}_{\text{DP}} \dot{g}_{\text{DP}} / \partial q_{\text{DP}} = \dot{\lambda}_{\text{DP}} k_{\text{DP}} \]

where \( g_{\text{DP}} \) and \( \dot{\lambda}_{\text{DP}} \) being the plastic potential and plastic multiplier, respectively, while \( q_{\text{DP}} \) is a stress like hardening parameter. Similar definition for the MR part leads to

\[ k_{\text{MR}} = \dot{\lambda}_{\text{MR}} \]

Finally, \( s_{\text{DP}} \) and \( s_{\text{MR}} \) are the constant viscosity moduli in compression and in tension, respectively.

In the damage part of the model, the highly asymmetric behavior of rocks in tension and compression is accounted for by separate scalar damage variables in these stress regions. The damage is chosen to be driven by the viscoplastic strain. By this choice and the perfect viscoplasticity assumption made above, both the strength and the stiffness degradation both in tension and in compression are governed by the damage part of the model. The damage part of the model is defined by equations...
\[
\omega_i = A_i \left(1 - \exp(-\beta_i \varepsilon_{eqt}^{vp})\right), \quad \omega_c = A_c \left(1 - \exp(-\beta_c \varepsilon_{eqc}^{vp})\right)
\]
\[
\dot{\varepsilon}_{eqt}^{vp} = \sqrt{\sum_{i=1}^{3} (\dot{\varepsilon}_{eqt}^{vp})^2}, \quad \dot{\varepsilon}_{eqc}^{vp} = \sqrt{\sum_{i=1}^{3} (\dot{\varepsilon}_{eqc}^{vp})^2}, \quad \dot{\varepsilon}^{vp} = \dot{\varepsilon}_{eqt}^{vp} \frac{\partial f_{\text{int}}}{\partial \sigma} + \dot{\varepsilon}_{eqc}^{vp} \frac{\partial g_{\text{op}}}{\partial \sigma}
\]
\[
\sigma = (1 - \omega_t)\bar{\sigma}_t + (1 - \omega_c)\bar{\sigma}_c \quad (\bar{\sigma} = \bar{\sigma}_t + \bar{\sigma}_c)
\]
\[
\bar{\sigma}_t = \max(\bar{\sigma}, 0), \quad \bar{\sigma}_c = \min(\bar{\sigma}, 0)
\]
\[
\bar{\sigma} = E : (\varepsilon_{\text{tot}} - \varepsilon_{\text{vp}} - \varepsilon_0), \quad \varepsilon_0 = \alpha \Delta \theta I
\]

where parameters \(A_t\) and \(A_c\) control the maximum values of the damage variables \(\omega_t\) and \(\omega_c\) in tension and in compression, respectively. Parameters \(\beta_t\) and \(\beta_c\) are defined based on the fracture energies \(G_{Ic}\) and \(G_{IIc}\) by
\[
\beta_t = \frac{\sigma_0 h}{G_{Ic}} \quad \beta_c = \frac{\sigma_0 h}{G_{IIc}}
\]
with \(h\) and \(\sigma_0\) being a characteristic length of a finite element and the uniaxial compressive strength of the material, respectively. The equivalent viscoplastic strain in tension and compression, \(\varepsilon_{eqt}^{vp}, \varepsilon_{eqc}^{vp}\), respectively, are defined by the rate of viscoplastic strain tensor \(\varepsilon_{\text{vp}}\) and its principal values \(\varepsilon_{\text{vp}}^i\). Moreover, the nominal-effective stress relationship is written in (2) with the positive-negative parts decomposition. Finally, the (effective) stress-strain relationship is written with the elasticity tensor \(E\) and the total (\(\varepsilon_{\text{tot}}\)), viscoplastic (\(\varepsilon_{\text{vp}}\)) and thermal (\(\varepsilon_{\theta}\)) strain tensors. In the definition of the thermal strain, \(\alpha\) and \(\Delta \theta\) are the thermal expansion coefficient and the temperature change, and \(I\) is the second order identity tensor.

2.2. Explicit time stepping scheme for solving the discretized thermo-mechanical problem

This problem of thermal jet drilling is governed by the time-dependent heat equation and the equation in motion, which can be written in the finite element discretized form as

\[
\begin{align*}
C \dot{\theta} + K_\theta \theta &= f_\theta + f_\theta^f & \text{(3)} \\
M \dot{u} + f_\text{int} &= f_\text{ext}
\end{align*}
\]

where the symbol meanings are as follows: \(\dot{u}\) is the acceleration vector; \(\theta\) is the temperature vector; \(M\) is the lumped mass matrix; \(f_\text{ext}\) is the external force vector; \(f_\text{int}\) is the internal force vector; \(C\) is the capacitance matrix; \(K_\theta\) is the conductivity matrix; \(f_\theta\) is the external heat loading vector; \(f_\theta^f\) is the mechanical heating term (vector); \(A\) is the standard finite element assembly operator; \(B_\theta\) is the kinematic matrix (mapping the nodal displacement into element strains); \(\rho\) is the density; \(c\) is the specific heat capacity; \(\theta\) is the temperature; \(N_\theta\) is the temperature interpolation matrix; \(k\) is the conductivity; \(q_n\) is the normal component of the heat flux; \(B_\theta\) is the gradient of \(N_\theta\).

Generally, the specific heat capacity and conductivity depend on temperature but this dependence is neglected in this study. Moreover, the convection at the boundaries of the rock specimen is also ignored as insignificant due to the short heating times. Finally, the term \(Q\) in (3) expresses the mechanical heat production through dissipation and strain rate. This term is ignored as insignificant in comparison to the external flux, hence \(f_\theta^f \equiv 0\) from now on. This assumption makes the thermo-mechanical problem uncoupled facilitating its solution significantly.

The uncoupled thermo-mechanical problem governing the thermal spallation is solved with explicit time marching. The forward Euler method is employed here leading to following equation for the new temperature and mechanical response:
\[ C_j \theta_{r_j} + \Delta t(f_{ext} - K_{ij} \theta_i) \rightarrow \theta_{r_j} \]

\[ \dot{\mathbf{u}} + f_{ext} \rightarrow \ddot{\mathbf{u}} \]

\[ \ddot{\mathbf{u}}_{r_j} = \dot{\mathbf{u}}_{r_j} + \Delta \dot{\mathbf{u}}_{r_j} \]

\[ \mathbf{u}_{r_j} = \mathbf{u}_{r_j} + \Delta \mathbf{u}_{r_j} \]  

Now, the solution procedure is as follows. First, the temperature at time \( t + \Delta t \) is solved by Equation (4)1. Second, an element level loop is performed for solving the new stress, viscoplastic strain, and other internal variables based on which the new internal force vector is assembled. Third, the acceleration \( \ddot{\mathbf{u}}_i \) at \( t \) is solved with (4)2. Finally, the mechanical response, i.e. displacement \( \mathbf{u}_{r_j} \) and velocity \( \dot{\mathbf{u}}_{r_j} \), is computed further.

3. Numerical examples

The numerical simulations of the thermal spallation drilling are carried out here. As mentioned, the rock heterogeneity is accounted for by random clusters of finite elements representing three different rock constituent minerals. The material parameters for the minerals used in the simulations are given in Table 1. These values, mainly taken from [7-10], do not necessarily represent any particular rock but a granitic rock like behaviour in general.

| Parameter/mineral | Quartz | Feldspar | Biotite |
|-------------------|--------|----------|---------|
| \( E \) (GPa)     | 80     | 60       | 20      |
| \( \sigma_0 \) (MPa) | 10     | 8        | 7       |
| \( c_0 \) (MPa)   | 50     | 50       | 50      |
| \( v \) (Poisson’s ratio) | 0.17   | 0.29     | 0.2     |
| \( \varphi \) (°) | 30     | 30       | 30      |
| \( \psi \) (°)    | 5      | 5        | 5       |
| \( \rho \) (density) (kg/m³) | 2703   | 2703     | 2703    |
| \( G_{IC} \) (J/m²) | 40     | 40       | 28      |
| \( G_{IIc} \) (J/m²) | 400    | 400      | 280     |
| \( A_f \)         | 0.98   | 0.98     | 0.98    |
| \( A_c \)         | 0.95   | 0.95     | 0.95    |
| \( s_{MR} \) (MPa-s) | 0.01   | 0.01     | 0.01    |
| \( s_{DP} \) (MPa-s) | 0.01   | 0.01     | 0.01    |
| \( \alpha \) (1/K) | 1.60E-5 | 0.75E-5  | 1.21E-5 |
| \( k \) (W/mK)    | 4.94   | 2.34     | 3.14    |
| \( c \) (J/kgK)   | 731    | 730      | 770     |
| \( f \) (%)       | 33     | 50       | 17      |

Some of the material parameters are assumed temperature dependent. Namely, the Young’s modulus \( E \), tensile strength and the cohesion are assumed to depend linearly on temperature so that their values at 500°C are 50% of the corresponding values at room temperature (293°K). Mathematically, the temperature dependence of a property \( x \) is thus of form \( x(\theta) = x(\theta_0) + k x \Delta \theta \)
where $K_s^{500}$ is the modulus of temperature dependence and $\theta_{ref}$ is reference temperature. This simplified assumption is sufficient for the present purpose to reveal the effect of temperature dependence of these parameters in general, not its exact quantification for a specific rock. However, the temperature dependence of the thermal expansion coefficient of some concretes [7] and granites [10] is nonlinear. Here, the data in [10] is approximated here by a second order polynomial $\alpha(\theta) = a_1\theta + a_2\theta^2 + a_3$ where $a_i$ are the fitting coefficients.

Due to the vastly differing time scales, and hence the critical time steps of the explicit schemes (4), of the thermal and mechanical problems, mass scaling is used to speed up the solution in time. Moreover, in order for the thermal spallation drilling to be competitive with the mechanical drilling methods, very short heating times and high heat flux intensities must be used.

The boundary conditions and a detail of the mesh used in the simulations are shown in figure 2. The axisymmetric mesh has 17399 standard four-node bilinear 2D solid elements and the average mesh size close to the nodes where the heat flux in applied is 0.25 mm. Different colors indicate different rock constituent minerals.

Finally, the heating time and the flux intensity are, respectively, specified as 0.05 s and $q = 7$ MW/m$^2$ as a flux intensity. These values are chosen for demonstrative purposes only and they do not necessarily represent practical values.

![Figure 2. Boundary conditions and a close up of the finite element mesh for thermal spallation simulations.](image)

Simulation results for the boundary conditions and mesh shown in figure 2 are presented in figure 3. According to the results in figure 3, the temperature at some of the flux nodes reaches 800°C, which is well beyond the $\alpha$ - $\beta$ quartz phase transition temperature (573°C), which is not taken into account in the present modelling. Moreover, due to the short heating time, the temperature rises notably only in a very narrow (< 1 mm) strip. However, the stress state this extremely steep temperature gradient induces in the rock surface area is enough to cause spalling, as attested in figure 3 by the bulging out of the strip of elements adjacent to the rock surface.
Expectedly then, this bulging is effectively prevented by confining pressure. The simulation results for the case with 20 MPa of hydrostatic confinement are shown in figure 4.

Hydrostatic confinement of 20 MPa indeed suppresses the tensile damage, as the tensile stresses induced by the heat shock are not enough to counter the compressive confinement. However, fully hydrostatic confinement may not always be the case in real in-situ drilling.
4. Conclusions

The numerical study on the effect of confinement on the action of thermal jet drilling demonstrates that (hydrostatic) confinement has a detrimental effect on the tensile damage required for the thermal spallation phenomenon to occur. According to the simulations carried out here, confinement of 20 MPa, corresponding roughly to a depth of 1 km, prevents spallation. While fully hydrostatic confining conditions are seldom met in real in-situ drilling, this results means, in the least, that the thermal jet drilling faces serious challenges in deep hole drilling. Finally, it is noted that despite the fact that the simulations were carried on granitic rock, the modelling approach can be applied to other rocks as well.

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References

[1] Kant M A and von Rohr PR 2016 Minimal Required Boundary Conditions for the Thermal Spallation Process, *Int J Rock Mech Min Sci*. Vol 84 p 177-186

[2] Rossi E, Kant M A, Madonna C, Saar MO, von Rohr P R 2018 The Effects of High Heating Rate and High Temperature on the Rock Strength: Feasibility Study of a Thermally Assisted Drilling Method. *Rock Mech Rock Eng*. Vol 51 p 2957-2964

[3] Rossi E, Kant M A, Borkeloh O, Saar M O, von Rohr P R 2018 Experiments on Rock-Bit Interaction during a Combined Thermo-Mechanical Drilling Method. *PROCEEDINGS, 43rd Workshop on Geothermal Reservoir Engineering*, Stanford University, Stanford, California, February 12-14

[4] Saksala T, 2018 Numerical modelling of thermal spallation of rock. In: *Proceedings of XII Argentine Congress on Computational Mechanics (MECOM2018)*, 6-9 November, Tucuman, Argentina

[5] Walsh S C, Lomov I N, 2013 Micromechanical modeling of thermal spallation in granitic rock. *Int J Heat Mass Transfer* Vol 65 p 366–373

[6] Saksala T, 2010 Damage–viscoplastic consistency model with a parabolic cap for rocks with brittle and ductile behavior under low-velocity impact loading. *Int J Numer Anal Meth Geomech*. Vol 34 p 1362–1386

[7] Ottosen N S and Ristinmaa M 2005 *The Mechanics of Constitutive Modeling*, Elsevier

[8] Park J-W, Park C, Ryu D and Park E S 2015 Numerical simulation of thermo-mechanical behavior of rock using a grain-based distinct element model. In: Schubert & Klueckner *Proceedings of EUROCK 2015 & 64th Geomechanics Colloquium*, Salzburg, Austria

[9] Tufail M, Shahzada K, Gencturk B and Wei J 2017 Effect of Elevated Temperature on Mechanical Properties of Limestone, Quartzite and Granite Concrete. *Int J Concr Struct M* Vol 11 p 17-28

[10] Heuze F, 1983 High Temperature Mechanical, Physical and Thermal Properties of Granitic Rocks – A Review. *Int J Rock Mech Min Sci & Geomech Abstr* Vol 20 p 3-10