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Author(s):
Roethlin, Christina; Calvetti, Francesco; Yamaguchi, Satoru; Vogel, Thomas

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Numerical simulation of rockfall impact on a rigid reinforced concrete slab with a cushion layer

C. Roethlin¹*, F. Calvetti²*, S. Yamaguchi³ and T. Vogel¹

¹ETH Zurich, Switzerland, ²Politecnico di Milano, Italy, ³Civil Engineering Research Institute for Cold Region, PWRI, Japan,*Co-first authors

Abstract
Rockfall protection galleries typically consist of reinforced or prestressed concrete slabs that are covered by a granular cushion layer. In general, the cushion layer prolongs the contact duration and reduces the maximum force of any impact. The magnitude of these effects depends on the characteristics of the cushion and the input quantities of the impacting body. This paper describes a numerical study, which employed a three-dimensional Distinct Element Method (DEM), of the mechanical behaviour of granular cushion layers placed on a rigid reinforced concrete slab under the effects of rockfalls. The analysis highlights fundamental loading characteristics, such as the force and duration of impact as well as the stresses acting on the slab. These simulations used the reference of a series of physical impact tests conducted on a rigid reinforced concrete slab covered by a sand cushion. In the initial step of the analysis, simulations of the experiments were conducted by assuming a preliminary choice of the contact parameters. This choice was based on the available information regarding the type of sand, the thickness and the density of the layer used in the experiments. These simulations are therefore blind predictions of the experiments. Parametric studies with reference to numerical damping and micromechanical parameters demonstrated that the numerical approach correctly predicted the relevant aspects of the impact loading phenomena. The observed stress distribution on the slab was shown to be highly dependent on the shape and size of the impacting body. These effects of the observed loading characteristics are imperative for evaluating the expected dynamic structural response – particularly the phenomenon of punching shear – of a gallery slab.

Keywords: Rockfall impact, cushion material, Distinct Element Method, blind prediction

¹Institute of Structural Engineering, ETH Zurich, Wolfgang-Pauli-Strasse 15, 8093 Zurich, Switzerland.
roethlin@ibk.baug.ethz.ch &Tel.: +41-44-633 31 44,
vogel@ibk.baug.ethz.ch &Tel.: +41-44-633 31 34
²Department of Structural and Environmental Engineering, Politecnico di Milano, 20133 Milano, Italy. francesco.calvetti@polimi.it & Tel.: +39-0341-488819
³Civil Engineering Research Institute for Cold Region, PWRI, Japan.
Yamaguchi-s22aa@ceri.go.jp & Tel.: +81-11841-1698
1 Introduction

The integral task of rockfall protection requires a multidisciplinary approach for modelling several aspects of the phenomenon (triggering, trajectories, protection works, etc.) [1]. Analysing the structural performance of reinforced concrete slabs under rockfall impact requires an understanding of the dynamic behaviour of the granular material placed on the structure and of the reinforced concrete structure itself.

This paper takes into account the discrete material characteristics by using a micromechanical approach for performing a numerical analysis of the dynamic behaviour of granular material. The objective was to model the macroscopic behaviour of rockfall impacts on granular cushion material placed on a reinforced concrete slab. The results of physical experiments on the absorbing performance of sand cushion layers conducted in Japan [5] are used as a reference for the numerical simulation by the Distinct Element Method (DEM) [3]. The granular assembly is modelled as a collection of rigid particles. The mechanical behaviour of the granular material is characterised by its porosity, a simplified grain-size curve, an assumption of round particles (no rotations) and an appropriate choice of the mechanical micro–macro interactions [2]. The choice of micromechanical parameters is based on the available information regarding the type of sand used in the experiments.

First, a blind prediction of the experimental results was performed. A comparison of the extended parametric study to the experimental results, with respect to micromechanical parameters and local damping, revealed that the numerical approach correctly described the relevant aspects of the impact tests. The calibrated numerical model was used to investigate additional aspects of the impacts, such as the influence of the shape of the impacting body and the stress distribution on the slab. Finally, the effects of the observed loading characteristics on the expected dynamic structural response, especially with respect to the phenomenon of punching shear, are discussed.

2 Outline of the physical experiment

The physical experiments [5] were carried out on a rigid reinforced concrete slab (5 m x 5 m x 0.5 m) covered by a sand cushion layer. The slab was supported by a steel and reinforced concrete structure and its deformability can be neglected. A side and a top view of the experimental setup are shown in Figures 1 (a) and (b), respectively.

![Figure 1](https://example.com/figure1.png)

Figure 1. Experimental setup: (a) side view and (b) top view with load cells. Dimensions in mm.
Twenty-nine load cells were placed on the concrete slab according to the arrangement shown in Figure 1. Five load cells with a nominal capacity of 750 kN each (3750 kN in total) were placed directly below the impact area. Eight load cells with a nominal capacity of 500 kN each (4000 kN in total) and 16 load cells with a nominal capacity of 300 kN each (4800 kN in total) were distributed over the slab as illustrated in Figure 1 (b). Three cushion thicknesses, 0.3 m, 0.5 m and 0.7 m, were used. The mass of the impacting body was 5 t. The shape of the body is illustrated in Figure 2 (a). Accelerometers were mounted on the impacting body as shown in Figure 2 (a). Impact tests were performed from heights of 1.0 m up to 15.0 m. The energy domain therefore ranges between 49 kJ and 736 kJ.

Table 1 summarises the parameters, including the falling height and cushion characteristics, of the experimental cases. The maximum and minimum void ratios are $e_{\text{max}} = 1.256$ and $e_{\text{min}} = 0.723$. Case number 11 is a repetition of case number 4. The relative density ranges from 12\% (loose sand) to 60\% (medium-dense sand). The corresponding grain-size curve is shown in Figure 2 (b).

Table 1: Experimental cases and corresponding falling height $H$, cushion thickness $d$, water content $w$, wet density $\rho$, void ratio $e$, porosity $n$ and density index $D$.

<table>
<thead>
<tr>
<th>No.</th>
<th>$H$ [m]</th>
<th>$d$ [m]</th>
<th>$w$ [%]</th>
<th>$\rho$ [t/m$^3$]</th>
<th>$e = G_s(1+w)(\rho/\rho_s)-1$ [-]</th>
<th>$n = e/(1+e)$ [%]</th>
<th>$D = (e_{\text{max}}-e)/(e_{\text{max}}-e_{\text{min}})$ [%]</th>
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Figure 2. (a) Dimensions of the impacting body (steel) in mm. (b) Grain-size curve.
3 The Distinct Element Model

3.1 Modelling approach

Particle Flow Code (PFC3D, Itasca Consulting Group, Inc, 2003, [4]), a 3D Distinct Element Code, was used for the numerical simulation of the impact experiments. This simulation used spherical elements in a modelling approach, as developed by Calvetti [2]. The interactions between the spherical elements are governed by simple elasto-plastic contact constitutive laws, which are characterised by the interparticle friction angle ($\phi_u$) as well as normal and tangential contact stiffness ($k_n$ and $k_s$). Accurate simulation relies on the proper reproduction of the mechanical material behaviour as characterised by the porosity and the grain-size curve.

For spherical particles, if particle rotations are allowed, a limit value of the macroscopic friction angle is attained irrespective of the magnitude of the interparticle friction angle (Figure 3 (b)) [6]. This limit value depends on the particle-size distribution and is in general smaller than $30^\circ$, which is lower than friction angles observed in actual geomaterials. Particle rotation was therefore restricted in the simulations. Restricting particle rotation imposes a constraint that is similar to adopting an infinite rotational stiffness for the particles and therefore negates the influence of the actual particle shape. Restricting particle rotation does not correspond to the real physical behaviour of soil. However, if particle rotation is restricted in combination with the choice of an adequate value of the interparticle friction angle, a good reproduction of the global behaviour of the model is achieved [2].

![Figure 3. Micromechanical parameters (a) contact model in PFC3D and (b) numerical results of contact friction angle $\phi_u$ and dilatancy angle $\psi$. Modified according to [6].](image)

The Distinct Element Model (DEM) is intended to be as simple as possible in order to minimise the micromechanical parameters needed in the calibration process rather than attempting to reproduce the real grain-to-grain interaction. As a consequence, the model requires the evaluation of only two micromechanical parameters, namely interparticle stiffness and interparticle friction. In previous simulations of the mechanical behaviour of several types of sand (the Ticino river, Hostun and Adige river) [2], contact stiffness was found to have a negligible influence on the strength of the granular material and a simple linear relationship between micro- and macromechanical strength parameters ($\phi_u$ and $\phi$) was observed [2]. For the considered types of sand, the contact friction lies in the range of $0.3 < \mu = \tan(\phi_u) < 0.35$, which corresponds to $16.7^\circ < \phi_u < 19.3^\circ$ for the investigated range of porosity ($0.4 < n < 0.5$). The normalised contact stiffness $k_n/D$, where $D$ denotes the particle diameter, determines the overall stiffness $E$ of the assembly. An empirical relationship between the normalised contact stiffness and overall stiffness
of the assembly was obtained with $k_c/D = (2-2.5)E$. For these simulations the ratio $k_c/k_n$ was kept constant at 0.25. The normalised stiffness was in the range of $250 \text{MPa} < k_c/D < 420 \text{MPa}$. The normalised contact stiffness $k_c/D$ does not affect the macroscopic friction angle $\phi$, and in turn the interparticle friction angle $\phi_p$ does not affect the macroscopic stiffness $E$.

3.2 Setup of the numerical model

The particle assembly was randomly generated and compacted within a set of confining walls (Figure 4 (a)). The model boundaries were defined before generating the particles as they assist with the generation and compaction of particles. The bottom of the box was a square wall of 25 $\text{m}^2$. The height of the box was 0.3 m, 0.5 m and 0.7 m, which corresponded to the cushion thicknesses $d$. The bottom wall was divided into cells in order to calculate the stresses acting on the concrete slab. The numerical specimens featured a uniform grain-size curve, which was based on the grain-size curve of the sand shown in Figure 2 (b), linearised in the diameter range 0.15 mm to 0.55 mm. Preliminary numerical investigations aimed to optimize the particle size and reduce the computational time. In order to reduce the calculation time, the discrete particle size was increased and the number of particles was reduced by an appropriate scaling factor [7]. The influence of five different scaling factors on the time history of the impact force and the penetration of the impacting body for a cushion thickness of $d = 0.7$ m and a falling height of $H = 5.0$ m were investigated as shown in Figure 4 (c). In these simulations the porosity was chosen as $n = 41\%$. For a scaling factor of 400, the sand was represented by 6,090 spheres, whereas 70,842 spheres were generated for a scaling factor of 180 (layer thickness of 0.7 m). The results in Figure 4 (c) show that a scaling factor of 400 was inaccurate.

![Figure 4](image_url)

Figure 4. (a) Setup of the confining walls from the numerical model in PFC$^{3D}$, (b) nomenclature and (c) preliminary investigation for $d = 0.7$ m, $H = 5$ m: impact force $F(t)$ and penetration depth $p(t)$. 
Deviation from the other results was particularly evident when considering time history of the penetration depth $p(t)$, which exhibited a clear rebound. The results also indicated that scaling factors of 180 and 200 yield similar time histories and therefore only the scaling factor of 200 was used. For a scaling factor of 200, the total number of spheres generated for the cushion thicknesses 0.3 m, 0.5 m and 0.7 m were 19'312, 32'854 and 41'217, respectively. For a series of simulations devoted to the study of the stress distribution on the slab, an even larger number of particles in contact with the slab itself were required for a reliable evaluation of the stresses and therefore a scaling factor of 120 was used (see § 6).

4 Results of blind prediction and comparison to experimental results

Results from the study by Calvetti [2] were used to select typical values for the micromechanical parameters for the blind prediction of the impact experiments. A contact friction of $\mu = 0.325$ ($\phi_0 = 18^\circ$, $\phi = 35^\circ$) was assigned to the particles. A tentative value of 330 MPa was assigned to the normalised contact stiffness $k_n/D$ and the shear contact stiffness was taken as $k_s = 0.25 \cdot k_n$. It was assumed that the walls have the same normal and shear contact stiffness as the spherical elements. Contacts were also given a small tensile and cohesive resistance in order to match the low porosity of the cushion layer employed in the experiments. This type of bond strength accounted for the existing amount of water in the cushion. Because the bond strength was much smaller than the forces that arise during impacts, it did not influence the behaviour of the layer under impact. Local damping of 5% was added in the blind prediction.

All the numerical results are plotted in Figure 5.

Figure 5. Blind prediction of impact force $F(t)$, transmitted force $F^* (t)$ and penetration depth $p(t)$ for cushion thicknesses (a) $d = 0.3$ m, (b) $d = 0.5$ m and (c) $d = 0.7$ m.
In general, the model accurately predicted the experimental results. The main discrepancies arose when the smallest layer thickness \(d = 0.3\, \text{m}\) was considered. This was probably due to the limited number of particles over the height of the layer. The maximum impact force, transmitted force and penetration depth increased with increasing falling height, as shown in Figure 6. With decreasing falling height, the peak values of the impact force \(F_{\text{max}}\) and the transmitted force \(F^*_{\text{max}}\) were progressively delayed. A large maximum penetration depth was observed for all cases, indicating that the sand cushion was rather loosely packed.

Figure 6. Comparison of blind prediction to experimental results for maximum values of (a) penetration depth \(p_{\text{max}}\), (b) impact force \(F_{\text{max}}\) and (c) transmitted force \(F^*_{\text{max}}\).

Figure 7 illustrates a comparison between the time histories of impact force \(F(t)\), transmitted force \(F^*(t)\) and penetration depth of the impacting body \(p(t)\) recorded during the experiments and the simulations. Only a number of impacts were selected in order to illustrate the performance of the model. For this comparison local damping was neglected.

Figure 7. Comparison of the numerical results with the experimental results.
5 Parametric study

Comparison of the numerical simulations with the experimental results showed that the model accurately predicted the relevant aspects of the impact loading phenomena. Extended parametric studies for case number 10 investigated the influence of numerical damping and micromechanical parameters. The results, illustrated in Figure 8 (a), suggested that the numerical model could be improved. However, the results clearly showed that the refined model does not significantly improve the results.

(a) Parametric studies ($H = 15$ m, $d = 0.7$ m)

(b) $d = 0.7$ m

Figure 8. Calibration of the numerical model (a) parametric study on the transmitted force $F^*(t)$ for input quantities corresponding to case number 10 and (b) best fit for comparison to experimental results of impact force $F(t)$.

For the calibration of the numerical model, a reduced scaling factor of 120 was chosen in order to accurately predict the stress distribution in the vicinity of the impact location and to improve the evaluation of the dynamic behaviour of the cushion layer. As previously indicated, local damping, interparticle stiffness and the internal friction angle were calibrated. The normalised stiffness was optimised and it was found that a value of $k_n/D = 150$ MPa gave satisfactory results. As shown in Figure 8 (a), the normalised stiffness influenced mainly the first phase of the time history of impact force. This phase corresponded to the time before the reflection of the compressive wave on the bottom wall. The influence of local damping and the internal friction angle were then studied. In the case of a normalised stiffness of $k_n/D = 150$ MPa, local damping was found to affect mainly the characteristics of the second phase of the load time history and therefore the time duration and magnitude of the load after the reflection of the compressive wave on the bottom wall. Damping was found to be inversely proportional to the magnitude of the second peak load and directly proportional to the time duration. The internal friction angle mainly influenced the time duration of both peak loads and the magnitude of the first peak load. The internal friction angle was found to be directly proportional to the magnitude of the first peak load and inversely proportional to the duration of each peak. A smaller internal friction angle was also associated with an increased time between both maximum peak loads.

The parametric study demonstrated that the numerical approach correctly predicted the relevant aspects of the impact loading phenomena. The time histories of the impact $F(t)$ and transmitted
force $F^*(t)$ were accurately predicted for all experimental cases. The effects due to the propagation of the compressive wave and its reflection at the bottom wall influenced the results of the second phase of the time histories for impact and transmitted force. The parametric study revealed that a scaling factor of 120, a normalised contact stiffness of $k_c/D = 150$ MPa and a corresponding shear contact stiffness of $k_s = 0.25 \cdot k_c$ provided the best fit to the experimental results. The internal friction angle was chosen as $\mu = 0.3$ for cushion thicknesses $d = 0.5$ m and $d = 0.7$ m, and $\mu = 0.35$ for $d = 0.3$ m. Local damping was chosen as 0.03 for $d = 0.7$ m and 0.045 for $d = 0.5$ m and $d = 0.3$ m, as shown in Figure 8 (b).

6 Stress distribution

The effects of the loading characteristics observed in the numerical analysis are discussed with respect to the expected dynamic structural response of a gallery. The numerical model was used to investigate additional aspects of the impacts, such as the influence of the shape of the impacting body (Figure 9 (a)) and the stress distribution on the slab (Figure 9 (b)). As shown in Figure 9 (a), two different shapes of body – rounded base and flat base – were used to investigate the influence on the stress distribution on the slab (Figure 10). As the stresses were computed for each quadratic mesh, the particles had to be scaled with a reduction factor of 120 in order to represent the actual impact phenomena with the necessary amount of particles per grid pattern. Transmitted force $F^*(t)$ and stress distribution $\sigma$ were influenced by the shape of the impacting body. The magnitude of the influence depended on the cushion thickness, mass and falling height. A comparison of the results of different shapes of the impacting body with cushion thicknesses $d = 0.5$ m and $d = 0.7$ m showed that the effect of the shape of the body was greater when smaller cushion thicknesses were used. When a body with a rounded base was used, the particles were able to rearrange and spread. In contrast, when a flat base was used, the particles could not rearrange but were compressed directly below the impact area. The stress distribution (peak values) in the relevant part of the second quadrant (Figure 9 (b)) is illustrated in Figure 10. The refined grid pattern in and near the impacted area allowed prediction of a continuous distribution of the peak stresses, depending on the distance from the impact $r_i$. It was observed that the calculated stress distribution was mostly concentrated directly under the impacting body. The stresses at the outer perimeter of the body (0.5 m from origin) were reduced by at least 70% when compared to the maximum stresses in the middle field of 0.25 m x 0.25 m.

Figure 9. (a) Shape of impacting body and (b) stress distribution. Dimensions in mm.
This study revealed that knowledge regarding the stress distribution and loading area of a slab are critical for evaluating local failure mechanisms (punching shear) in slabs subjected to large rockfall impacts. The effect of the size and shape of an impacting body on the angle of the shear cone within the cushion layer, and hence on the control perimeter of the slab, must be considered when evaluating the punching shear strength of reinforced concrete slabs.

Table 2: Percentage decrease of stresses on the slab compared to the middle field of 0.25 m x 0.25 m for case numbers 2, 6 and 10 for rounded base (rb) and flat base (fb).

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<th>Distance $r_i$</th>
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7 Conclusion

The Distinct Element Method was used to reproduce physical impact experiments on a sand cushion. The results demonstrated the reliability of the numerical modelling approach. Appropriately assigned micromechanical contact parameters and local damping allowed successful reproduction of the experimental results. The results also highlighted the important influence of stress distribution on the punching shear behaviour of slabs. Determination of stress distribution on the slab and the resulting loading area (control perimeter) are required for proposing design rules for punching shear. The influence of the loading area on global bending behaviour is of lesser importance.
8 Acknowledgements

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9 References