The Young Professionals Council (YPC) is a committee within the Southern African Institute of Mining and Metallurgy (SAIMM) which was established in 2014 to identify relevant issues and challenges being experienced by young professionals in the industry and to work towards finding solutions and assisting the future leaders in our industry. The work of the YPC is guided by the Constitution and By-law I of the SAIMM.

The YPC has eighteen elected committee members which is headed up by the Chairperson. There are seven portfolios who report directly to the Office Bearers of the YPC and are focused on Advocacy, Funding, Marketing, Membership, Publications, Strategic Partnerships and Technical Programmes Group, the Career Guidance Working Group and the Enterprise Working Group. The Communication Committee feeds into all the above structures to ensure that all communications are aligned with the principles of the YPC.

**Background**
- The YPC has identified six Focus Areas which are documented in the By-law.
- The six Focus Areas are managed through three Working Groups
- Each Working Group is led by a Champion and 2-I-C, appointed by the YPC Chairman
- A Working group may convene sub-groups as necessary to manage its activities.

**Education**
- The role in influencing schools – from career guidance, mathematics, science, tutoring, life skills, etc.
- The role in universities – from the selection processes to the alignment in getting the degree and its uses in the industry

**Purpose** — The Education WG represents the interests of primarily pre-graduates in basic and higher education on matters of career guidance, academic development and life skills.

**Career Guidance**
- The role of business – from best practice, training programmes, mentoring programmes, and development
- Influencing other bodies such as ECSA, DMR, AMMSA, etc. for the benefit of all young professionals

**Purpose** — The Career Guidance WG represents the interests of primarily post-graduates in mining and metallurgy on matters of training, professional development and life skills.

**Enterprise**
- Influencing service providers outside the mainstream who provide secondary services
- Instituting entrepreneurial activities that will serve the requirements of young professionals.

**Purpose** — The Enterprise WG shall primarily undertake industrious initiatives of some scope, complication and risk to serve the interests of young professionals.

**General Responsibilities of a Working Group**
- Strategizing
- Project planning and management
- Evaluation and monitoring the impact of the projects
- Feedback on projects
- Fundraising
- Advocacy support
- Contacts and Networking – locally, nationally and internationally.
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Updates and further development of previously published material, based on new data and better insights, are provided. This information can be used for mine planning and for the evaluation of long-term stability of the surface overlying coal workings.
The range of topics and the locations of the authors in this month's edition is an indication of the international nature of the Journal, which is confirmed by the fact that of the 141 papers that were published in the Journal in 2017/2018, 58 were from outside South Africa. This is one of the criteria that ensures that the Journal is listed on the Department of Higher Education and Training's (DHET) list of accredited journals.

A recent communication from a Retired Fellow contained the comment 'I remember when the Journal reflected the activity of the SAIMM - describing the events on a more personal level, interactions between members and news of members. Passionate written debates on the technical content were commonplace'. The Publications Committee have taken this to heart and wishes to encourage the readership of the Journal to provide constructive feedback for discussion on papers that are published in the Journal.

In the first 6 months of 2018, a total of 200 manuscripts were received by the publications team for consideration for publication in the Journal. This is in addition to the conference papers that had to be processed for publication and highlights the tremendous load that is placed on our referees. An important element in the refereeing process is the iteration that occurs between referee and author. A referee is asked to referee a paper based on his or her knowledge of and expertise in the subject matter. The referee’s comments and suggestions for changes and corrections to the manuscript are returned to the author. It is expected that the author will respond and give feedback to the referee that the comments and suggestions for changes and corrections have been noted and acted upon. It occasionally occurs that the author disagrees with the referee’s comments and the editor is called upon to mediate. These steps are an essential part of progressing a manuscript through the refereeing and reviewing process and ultimately producing a quality paper. If the author does not respond to the referee then time is wasted and the referee naturally becomes reluctant to referee any future papers. This situation has to be avoided and it is incumbent on authors to respond to the referees.

During the course of the year we have gradually migrated the reviewing of papers that are submitted to the Journal to the Open Journal System (OJS) which can be viewed at https://saimmjournal.co.za. Authors and reviewers are required to register on the OJS and this has enabled a substantially paper-free process to manage the progress of a paper from submission to publication. Refining of the system to suit all the needs of the Journal continues.

D. Tudor
A number of studies have confirmed that there is a positive relationship between diversity and business performance, and that diversity in leadership roles is what tends to define the success of a business. This is because knowledge creation and application is enriched by a variety of skills, experience, and cultural diversity. The more diverse a team, the more perspectives, the broader and more challenging the conversations, and the better the decisions that are finally taken.

In the past few years, the mining and minerals sector has changed significantly in terms of diversity in its workforce. However, there is always a great need to look beyond the numbers of diverse employees, or the diversity of the demographics, to inclusion. Inclusion is about recognizing, respecting, valuing and leveraging diversity in a way that enhances the operations of the company. The inclusion of voices from different geographic, gender, economic, and cultural groups shows workers that they are valued contributors to the company’s operations and performance. And it is through the creation of a workplace that is flexible, respectful, welcoming, and with employers that respond to individual needs and interests, that employees are able to express, create and nurture their ideas openly and hence contribute to the growth of the company. A company that embraces diversity and inclusion not only creates opportunities for individuals but also harnesses great talent and potential for itself.

In the 2017 November edition, I wrote about women in the mining sector. I highlighted some of the challenges that women face and indicated that the SAIMM needs to engage the mining sector leadership in promoting an environment that fosters the retention and growth of women in the sector. Whereas our sister institutes like the AusIMM and CIM have long realized the importance of such platforms, this is a space in which the SAIMM has not been significantly involved. However, I am happy to report that a committee that looks into such an environment and other relevant issues has been initiated by the Institute. The committee for Diversity and Inclusion in the Minerals Industry held its first meeting in April. The committee is made up of both men and women, young and experienced, who are passionate about seeing change in terms of diversity and inclusion in the minerals industry. The focus is on providing a platform, through SAIMM activities, to raise awareness on issues of diversity (gender, ethnicity, religion and other diversifying factors) and inclusion in the workplace for professionals in the minerals sector. Partnering with other like-minded groups such as Women in Mining South Africa (WiMSA) on occasion to deliver initiatives and activities to members is also envisioned. The committee will adopt a professional approach, with positive reinforcement involving both a top-down and a bottom-up approach, where effort is directed at companies, management, and students. This will be achieved through the presentation of technical papers at conferences, seminars, and running of workshops. Students, both male and (more specifically) females, are usually unprepared for the work environment and culture. This can adversely affect their performance and hence their integration and retention in the industry. The SAIMM can play a role by providing awareness to and educating the students on the industry culture and environment that they will be working in. For this year, presentations highlighting some of the diversity and inclusion issues are earmarked for the MineSafe and the Career and Leadership conferences. Such an approach of interacting with both the current and future leaders of the industry will go a long way towards fostering change in the minerals industry.

S. Ndlovu
President, SAIMM
There have been many descriptions offered of Oskar Steffen over the years: a statesman, a leader, a gentleman, a giant. He was all these things, and in the mining sector he became a legend on the strength of his knowledge, experience, and skilled professionalism. But perhaps the most important gift he gave was his respect for human dignity – which shone through not just in his personal relationships, but in business and the way he helped build SRK Consulting into a global business of the highest repute.

Master's degree in 1963. An early sign of his intellect and tenacity was that he spoke little English when he began university, having grown up and been to school in an Afrikaans-speaking environment. Another indicator was his swift progress in the workplace; starting work at Nchanga Consolidated Copper Mines in foundation engineering at Wits.

Hendrik Kirsten to form the Johannesburg-based firm of consulting engineers Steffen, Robertson and Kirsten – now a household name in the global mining sector but with reach and expertise well beyond the minerals industry.

So involved and well respected was the firm – and Oskar in particular – in the mining industry that he became President of the SAIMM in 1989–1990, perhaps one of the few presidents who were not mining engineers. It mattered not; Oskar was a mining man heavily involved in the day-to-day challenges of mines locally and abroad. He also had a far-sightedness that sought and found solutions that delivered inestimable financial benefit to the industry as a whole.

In his Presidential Address to the SAIMM in 1989, Oskar demonstrated this vision by touching not just on issues of demand for engineering skills and higher productivity, but on environmental protection and entrepreneurship in mining. In words that in retrospect encapsulate the modern approach to sustainability, he said: ‘Since the costs of all projects are ultimately recovered from the community for which they were undertaken, an over-engineered project may be just as damaging to the community as one that is under-engineered.’

He looked ahead to the evolution of South Africa’s mining sector, and seemed to foresee the rapid changes that would remould its corporate character so fundamentally during the 1990s. In his 1989 SAIMM address, he also noted: ‘The relative stability of our mining industry has resulted in secure and complacent attitudes over many years, which, by definition, are counter-productive to entrepreneurial development.’

One of his professional cornerstones was his teacher – and later colleague and mentor – at the Wits School of Mining Engineering, Professor Jere Jennings. An important collaborator in the evolution of Oskar’s ideas on rock slope stability, Professor Jennings also shared, and doubtless encouraged, Oskar’s people-centred approach.

These attitudes in many ways formed the cultural foundation of the world-class company that SRK Consulting was to become, encouraging a broad-based ownership among the strong contributors and securing their long-term commitment to the business. This was vital in creating a company that would embrace the changes of 1994 and transform its ranks in terms of gender, diversity, and age.

The SAIMM went on to bestow upon Oskar, in 1995, one of its highest honours: the Brigadier Stokes Memorial Award for ‘the very highest achievement’ in mining. Announcing the award, Rick Mohring (SAIMM President 1997–8) described Oskar as ‘the complete engineer’ – who was ‘concerned about the potentiality of failure and risk-evaluation methods, balancing this side always with cost.’ Mohring said that the SAIMM recognized him for ‘a combination of intellect, energy, professionalism, and humanness.’

In all this, Oskar was remarkable for his humility. Many people who knew him commented on his lack of ‘ego’ – often a rare attribute among mining men of his calibre. No less a feature was Oskar’s sense of humour, which helped him maintain a sense of perspective even in the many challenging situations his work required him to face.

Family, friends, and colleagues alike will all miss Oskar deeply, but we are comforted by the fact that his positive legacy continues in the lives and outlooks of everyone who encountered this extraordinary gentleman-engineer.
Virgin rock temperatures and geothermal gradients in the Bushveld Complex

by M.Q.W. Jones

Synopsis
Knowledge of virgin rock temperature (VRT) is essential for planning refrigeration and ventilation requirements of deep mines. Geothermal studies in the goldfields of the Witwatersrand have been in progress for approximately 80 years, with the result that a very extensive rock temperature database is available. However, it is only in the last three decades that deep-level platinum mining in the Bushveld Complex, north of the Witwatersrand Basin, has made mine refrigeration an important issue. This paper presents temperature profiles in 31 boreholes in the Bushveld Complex that were surveyed as part of a project, conducted between 1985 and 2005, aimed at establishing the geothermal characteristics of the Complex. Most of the boreholes are located in platinum mining areas or prospects situated around the perimeter of the Complex. The geothermal gradient in rocks of the Main Zone of the Complex, which immediately overlie the platinum reefs, is remarkably constant at 20.7±1.3 K/km. Data from the Upper Zone and the Bushveld granites yield lower gradients, 16.5±1.1 K/km and 16.4±2.4 K/km respectively. Geothermal gradients in the Bushveld Complex are approximately double the thermal gradients in the Witwatersrand. The geothermal heat flux in the platinum mining areas, calculated from the thermal gradients and thermal conductivity data measured on samples of borehole core, is also remarkably uniform at 45±4 mW/m²; this is somewhat less than the average for Witwatersrand gold mining areas, 51±6 mW/m². The main reason for the higher gradients in the Bushveld Complex is the low thermal conductivity of the rocks. Bottom-hole temperature measurements in boreholes in the Northam mining area of the Complex yield the highest virgin rock temperatures (up to 70°C at 2.2 km depth). This is 30°C hotter than the temperature at the same depth in mines in the Carletonville area of the Witwatersrand Basin. The surface temperature data and heat flow data, together with an extensive thermal conductivity database, make it possible to predict VRTs in new platinum mining areas as well as chromium and vanadium mines if such mining proceeds to substantial depths.

Keywords
Bushveld Complex, platinum mines, virgin rock temperature, geothermal gradient, heat flux.

Introduction
South Africa is endowed with an enormous mineral wealth, with the result that mining is one of the main pillars of the economy. Advances in technology in the last five decades have permitted mining at increasingly deeper levels. Increased mining depth means an increase in virgin rock temperature (VRT), which results in increased heat loads on underground workings (e.g. Rawlins, Phillips and Jones, 2002; Jones, 2003a). This is particularly relevant in the goldfields of the Witwatersrand, where mining approaches 4 km in depth, and in the Bushveld Complex, where platinum mines exceed 2 km depth. Because it is necessary to control the working environment, mine refrigeration and ventilation are important considerations when planning such deep-level mining. This planning requires knowledge of the VRT as well as the thermal properties of the rocks surrounding underground workings.

Gold has been mined in the Witwatersrand Basin (Figure 1) for more than 100 years, and geothermal research has been conducted for approximately 80 years. The earliest rock temperature studies (Weiss, 1938; Bullard, 1939; Krige, 1939) showed that the geothermal gradients are relatively low in the Witwatersrand. This has been confirmed by routine VRT measurements made in mines since 1950 down to maximum rock-breaking depth, where the VRT exceeds 65°C (Jones, 2003a). In the 1980s a detailed thermal investigation of the Witwatersrand Basin was conducted by the University of the Witwatersrand (Wits) in collaboration with the Chamber of Mines Research Organization (COMRO, now CSIR Mining Technology), and major mining companies. This resulted in an extensive compilation of VRT data in the basin and thermophysical properties of the main rock types. It also allowed for calculation of numerous determinations of the heat flux through the Earth’s crust. Details of the results are reported by Jones (1988, 2003a, 2003b).

During the Witwatersrand project, platinum mining in the Bushveld Complex north of the Witwatersrand Basin (Figures 1 and 2) was increasing in depth and it became obvious that a similar investigation of the Complex was necessary. The importance of this was made clear by two early measurements that indicated that the geothermal gradients are approximately...
Virgin rock temperatures and geothermal gradients in the Bushveld Complex

Figure 1—Locality of the Bushveld Complex (BC, black) and Witwatersrand Basin (WB, grey). The positions of mines in the Northam and Carletonville areas are also shown (in red)

double those in the Witwatersrand Basin (Carte and van Rooyen, 1969). The ensuing collaborative research project between Wits, COMRO, the Geological Survey of South Africa (now the Council for Geoscience) and major mining houses resulted in an extensive database constituting rock temperature measurements in boreholes distributed in the main mining areas, nearly 1000 measurements of the thermal properties of Bushveld rocks and 51 new values for the heat flux through the Earth’s crust. The latter two aspects of the project have been reported on in detail by Jones (2015, 2017).

This paper focuses on rock temperatures and geothermal gradients. It reports on temperature measurements made in boreholes situated in the main platinum mining areas as well as some in the interior of the Bushveld Complex. It discusses relevant aspects of thermal conductivity measurements on Bushveld rocks and determinations of heat flux. It provides a comparison with geothermal data from the Witwatersrand Basin and shows how the VRT in unexplored regions of the Complex can be estimated from existing geothermal data. This has implications for mine refrigeration studies for new platinum mines as well as future chromium and vanadium mines if they should become deeper.

Geological background

The geological terrain in which the Bushveld Complex is located is known as the Kaapvaal Craton. This Archaean cratonic nucleus, represented by basement granitic rocks and greenstone belts, developed between 3700 and 2650 Ma ago (Eglington and Armstrong, 2004), by which time it had largely stabilized. During this period, and subsequently, relatively undeformed stratified basins formed on the craton. The stratigraphy of these basins is shown in Figure 3. The oldest, the Witwatersrand Basin (Figure 1), hosts the largely volcanic Dominion Group (ca. 3100 Ma) (Marsh, 2007), overlain by the largely sedimentary Witwatersrand Supergroup (2600–2700 Ma) (van der Westhuizen, de Bruijn, and Meintjies, 2007). Subsequent deposition of platform strata of the Transvaal Supergroup (2650–2200 Ma) (Eriksson, Altermann and Hartzer, 2007) occurred in the Transvaal Basin, which overlies the Witwatersrand Basin and an extensive area of Archaean basement rocks further north. The next important geological event to affect this part of the Kaapvaal Craton was extrusion of the Rooiberg Group ca. 2070 Ma (Buchanan, 2007) and emplacement of plutonic rocks of the Bushveld Complex into the northern part of the Transvaal Basin at 2060 Ma (Figures 1 and 2) (Zeh et al., 2015; Mungall, Kamo and McQuade, 2016). Parts of the Witwatersrand Basin and the southern Bushveld Complex are covered by sedimentary rocks of the 300–200 Ma old Karoo Supergroup (Johnson et al., 2007).

The geology of the Bushveld Complex is shown in Figure 4, which is a sub-outcrop map showing the surface and inferred subsurface distribution of the main rock units below the Karoo cover (after Cairncross and Dixon, 1995). A simplified stratigraphic column of the Complex is shown in the right side of Figure 3.

The Bushveld Complex is a huge igneous province that occupies an area of more than 65 000 km$^2$ (Cawthorn et al., 2007). There are two main subdivisions, a lower, older mafic to ultramafic phase, known as the Rustenburg Layered Suite, and a younger, upper granitic phase comprising the Roshap Granophyre Suite and the Lebowa Granite Suite, usually referred to collectively as ‘Bushveld granite’ (Figures 3 and 4). The Rustenburg Layered Suite occurs in four main lobes (Figures 2 and 4). The western lobe extends from Pretoria to Thabazimbi, the northern lobe is mainly north of Mokopane, the eastern lobe from Zebediela to Stoffberg and the southern lobe (which is hidden by Karoo cover) extending as far south as Bethal.

Stratigraphically, the Rustenburg Layered Suite is conveniently divided into five zones (Figure 3). The Marginal Zone consists mainly of norite with some pyroxenite. The Lower Zone is ultramafic, dominated by pyroxenite, harzburgite, and dunite. The lower part of the Critical Zone is essentially pyroxenite, whereas the upper part is represented by cyclic layers of pyroxenite, norite and anorthosite. The Main Zone is predominantly norite and gabbro, with some layers of anorthosite and pyroxenite. The Upper Zone shows...
more variation in rock type and includes diorite, anorthosite, norite, gabbro and pyroxenite. The Bushveld granites intruded above this sequence.

The Bushveld Complex hosts the world’s largest reserves of platinum (and associated platinum group elements), chromium and vanadium (Cawthorn et al., 2007). The main units mined for platinum are the UG2 (Upper Group 2) chromitite layer and the Merensky Reef, which occur in the upper part of the Critical Zone in the eastern and western limbs of the Complex, and the Platreef in the northern limb. Because of its economic value, platinum mining of the UG2 and Merensky reefs now approaches depths of 2 km, which necessitates mine refrigeration. Current mining of the Platreef is by opencast methods, but underground mines are in the development stage. The main units mined for chromium are the LG6 and MG1 (Lower and Middle Group) chromitite layers, which occur deeper (stratigraphically) in the Critical Zone than the UG2 and Merensky Reef. Chromium mines are currently relatively shallow, but if mining proceeds deeper in the future, refrigeration will be required. Vanadium deposits are associated with magnetite-rich layers near the base of the Upper Zone in the eastern, northern, and western limbs of the Complex. Vanadium mines are also opencast at present but could go underground in the future.

**Virgin rock temperatures and geothermal gradients**

In addition to early borehole temperature measurements in the Witwatersrand Basin, most mining companies have conducted VRT measurements in mine workings since 1950. These data were collated by COMRO and the database provided valuable information regarding the depth to various isotherms in the Witwatersrand gold mining arc (Jones, 1988, 2003a). Although individual mining companies have made in-house VRT measurements in Bushveld mines, there is no similar coordinated VRT database for the Bushveld Complex. One of the main objectives of this investigation was to measure temperatures in exploration boreholes around the Complex in order to provide reliable estimates of the geothermal gradient for mines in the Complex. The methods of measurement and the results are described below.

**Borehole temperature measurement**

Temperature measurements were made in 31 boreholes (Table I) using electronic temperature probes attached to a cable and lowered by a portable winch. The temperature probes are similar to those described by Jones (1987, 1988). The sensor was either a thermistor or temperature transducer; both devices can detect temperature fluctuations of as little as 0.001 K, and calibration experiments ensured that absolute temperatures were accurate to within 0.01 K. The temperature at each depth was recorded after the probes had been held at the depth for two minutes, which is more than sufficient time for the probes to reach equilibrium. Temperatures were recorded at 10 m or 20 m depth intervals while the probes were being lowered. Depths were recorded using a calibrated depth counter attached to a pulley, over which the cable ran, and are estimated to be accurate to within 10 cm. Thermal gradients between successive temperature measurements were calculated from the field measurements.

Many of the boreholes are located in the platinum mining areas situated around the periphery of the Bushveld Complex, but there are some in the interior of the Complex (Figure 4, Table I). They are named after the original farms on which they are located and can be identified from their serial numbers, which appear in Tables I and II. All boreholes were
Virgin rock temperatures and geothermal gradients in the Bushveld Complex

Figure 4—Geological map of the Bushveld Complex and surrounding areas constructed from outcrop maps and inferred subcrop maps derived by stripping off the younger Karoo cover (after Cairncross and Dixon, 1995). Dots represent boreholes, and numbers refer to the site number listed in column 2 of Tables I and II.

Table I

<table>
<thead>
<tr>
<th>Site name</th>
<th>No.</th>
<th>Latitude</th>
<th>Longitude</th>
<th>Elev. (m)</th>
<th>Stratigraphic Unit</th>
<th>Area</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rustenburg*</td>
<td>1</td>
<td>-25.667</td>
<td>27.250</td>
<td>1160</td>
<td>Main Zone</td>
<td>Rustenburg</td>
</tr>
<tr>
<td>Umkoanesstad*</td>
<td>2</td>
<td>-24.313</td>
<td>27.900</td>
<td>832</td>
<td>Main Zone</td>
<td>Umkoanesstad</td>
</tr>
<tr>
<td>Schaapkrak SK2</td>
<td>3</td>
<td>-25.636</td>
<td>27.505</td>
<td>1120</td>
<td>Main Zone</td>
<td>Marikana</td>
</tr>
<tr>
<td>Schaapkrak SK1</td>
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<td>-25.632</td>
<td>27.440</td>
<td>1125</td>
<td>Main Zone</td>
<td>Marikana</td>
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<tr>
<td>Reinroyalskraal RK1</td>
<td>5</td>
<td>-25.596</td>
<td>27.278</td>
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<td>Main Zone</td>
<td>Rustenburg</td>
</tr>
<tr>
<td>Vlakfontein VLF1</td>
<td>6</td>
<td>-25.529</td>
<td>27.250</td>
<td>1127</td>
<td>Main Zone</td>
<td>Rustenburg</td>
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<tr>
<td>Doornspruit DS1</td>
<td>7</td>
<td>-25.481</td>
<td>27.201</td>
<td>1071</td>
<td>Main Zone</td>
<td>Rustenburg</td>
</tr>
<tr>
<td>Goedgedacht GG1</td>
<td>8</td>
<td>-25.427</td>
<td>27.160</td>
<td>1072</td>
<td>Main Zone</td>
<td>Rustenburg</td>
</tr>
<tr>
<td>Nooitgedacht NG1</td>
<td>9</td>
<td>-24.942</td>
<td>27.141</td>
<td>1005</td>
<td>Lower Zone</td>
<td>Middelwit</td>
</tr>
<tr>
<td>Elandsfontein EL1</td>
<td>10</td>
<td>-24.833</td>
<td>27.276</td>
<td>960</td>
<td>Main Zone</td>
<td>Amandelbult</td>
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<tr>
<td>Schildpadnek SKN1</td>
<td>11</td>
<td>-24.811</td>
<td>27.303</td>
<td>935</td>
<td>Main Zone</td>
<td>Amandelbult</td>
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<tr>
<td>Elandskull EK1</td>
<td>12</td>
<td>-24.759</td>
<td>27.369</td>
<td>895</td>
<td>Main Zone</td>
<td>Amandelbult</td>
</tr>
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<td>Zondereinde ZE1</td>
<td>13</td>
<td>-24.851</td>
<td>27.344</td>
<td>977</td>
<td>Main Zone</td>
<td>Northam</td>
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*Published values (Carte and van Rooyen, 1969)

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Virgin rock temperatures and geothermal gradients in the Bushveld Complex

Table II

<table>
<thead>
<tr>
<th>Site name</th>
<th>No.</th>
<th>Depth range (m)</th>
<th>Thermal gradient (K/km)</th>
<th>Main rock types</th>
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<td>$dT/dx$</td>
<td>Nr</td>
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<td>norite, gabro</td>
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<tr>
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<td>160–800</td>
<td>13.7–17.7</td>
<td>granite, gabbo</td>
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<td>Loskop North LKN1</td>
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<td>50–220</td>
<td>18.3</td>
<td>granite</td>
</tr>
</tbody>
</table>

$dT/dx$, thermal gradient; Nr, number of temperature measurements; *published values (Carte and van Rooyen, 1969); #thermal gradient after topographic correction

Results

Table I lists borehole localities, elevations, the main stratigraphic units in which temperatures were measured and the areas where the boreholes are located. Table II gives depth ranges displaying linear temperature profiles, average thermal gradients for these depths and the main rock types in which measurements were made.

Borehole positions are shown in Figure 4. Borehole numbers refer to column 2 of Tables I and II. The results of temperature measurements as functions of depth are shown in Figure 5. The latter results are arranged in stratigraphic order, with those in the uppermost rocks being presented first and those in the oldest rocks appearing last. The data in Figure 5 are colour-coded so that results from individual boreholes are evident. The geographic region in which the boreholes occur is indicated in Figure 2. The vertical bars on the temperature axes of Figure 5 indicate the temperature minimum relative to which the borehole data are plotted. Figures 5a and 5b present temperature profiles in Bushveld granite and the Upper Zone of the Complex, respectively. The data from the Main Zone (Figures 5c and 5d) are most relevant to this paper; they are arranged in clockwise order from Marikana through the western and northern limbs of the Complex to the Steelpoort region (Figure 5). Data from one borehole from the Lower Zone are given in Figure 5e. Figure 5f shows results from Transvaal Supergroup and Rooiberg Group lithological units occurring in geological inliers, or fragments, in the interior of the Complex (Figure 4).

The uppermost 100–200 m sections of many boreholes are characterized by a systematic concave temperature curve. This phenomenon is common in South Africa and elsewhere and is caused by increases in ground surface temperature of
Virgin rock temperatures and geothermal gradients in the Bushveld Complex

Figure 5—Temperature versus depth profiles for boreholes in the Bushveld Complex in the following order: (a) Bushveld granite, (b) Upper Zone, (c) and (d) Main Zone and upper Critical Zone, (e) Lower Zone and (f) fragments of pre-Bushveld rocks occurring in the interior of the Complex. Data from the Main–Critical Zones (c and d) are arranged in clockwise order from Marikana to Steelport. In order to conserve space, the temperature axes are arranged into ‘boxes’, the top boundaries of which are indicated by long vertical bars on the temperature axes; borehole temperatures are plotted relative to the top left value shown in each box. The area in which the boreholes occur is indicated diagonally in the top left hand corner of each box (see Figure 2 for area localities). Where there is more than one borehole in a particular area, the borehole data are colour-coded, as are the serial numbers. Borehole serial numbers (e.g. PAD1) and borehole numbers in parentheses (e.g. 14) are those listed in columns 1 and 2 of Tables I and II. Boreholes in the Moloto area pass through Bushveld granite and Upper Zone gabbro and these data are split between Figures 5a and 5b.

1–2°C during the last 200 years (Tyson et al., 1998; Jones, Tyson, and Cooper, 1999). Data showing such curvature was not used in the analyses. Below the curved zone, most boreholes display linear temperature profiles where the temperature data was obtained in a uniform rock type.

Discussion

Geothermal gradients in the Bushveld Complex

The thermal gradients listed in Table II are least-squares fits to the temperature versus depth data in the specified depth intervals. A range of gradients is given in cases where boreholes pass through variable lithologies with different thermal conductivities. The data confirm the early results of Carte and van Rooyen (1969), which indicated that the thermal gradient in the Bushveld Complex is relatively high.

Because of the great depth of some platinum mines, geothermal gradients above these mines are most pertinent. The gradient in individual boreholes, or range of values if more than one borehole was used, for different segments of the platinum mining areas or prospects around the perimeter of the Bushveld Complex are listed in Table III. In most cases the measurements were made in rocks constituting the Main and upper Critical Zones of the Complex, which overlie the main platinum reefs. The thermal gradient is remarkably uniform and averages at 20.7±1.3 K/km.

There is also a significant change vertically in the thermal gradient within the Bushveld Complex (Table IV). One
Virgin rock temperatures and geothermal gradients in the Bushveld Complex

<table>
<thead>
<tr>
<th>Area</th>
<th>(dT/dx) (K/km)</th>
<th>(q_0) [mW/m²]</th>
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<tbody>
<tr>
<td>Marikana</td>
<td>20.4–21.1</td>
<td>44–48</td>
</tr>
<tr>
<td>Rustenburg</td>
<td>19.3–24.5</td>
<td>46–51</td>
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<tr>
<td>Amandelburg</td>
<td>19.9–21.2</td>
<td>41–44</td>
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<tr>
<td>Northam</td>
<td>20.2</td>
<td>45</td>
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<tr>
<td>Vila Nova</td>
<td>22.6</td>
<td>42</td>
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<tr>
<td>Mokopane</td>
<td>18.2–20.9</td>
<td>43–47</td>
</tr>
<tr>
<td>Zebediela</td>
<td>17.7–19.9</td>
<td>40–44</td>
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<tr>
<td>Umkoanaasstad</td>
<td>21.2</td>
<td>47</td>
</tr>
<tr>
<td>Steelpoort</td>
<td>21.2</td>
<td>43</td>
</tr>
</tbody>
</table>

\(dT/dx\): thermal gradient; \(q_0\): heat flow

measurement in the Lower Zone yielded a gradient of 16.0 K/km (NG1). Eight measurements in the Upper Zone yielded 16.5±1.1 K/km, and three in Bushveld granite yielded 16.3±2.4 K/km.

### Thermal conductivity and heat flow

Jones (2015) recently reported on thermal conductivity measurements of more than 900 samples of all major rock types constituting the Bushveld Complex. Average values are listed in Table V. This table also summarizes results of measurements on more than 1000 samples of rocks from the Witwatersrand area (Jones, 2003b). As such, this table represents an almost complete ‘thermal conductivity stratigraphy’ of the rock groups represented in Figure 3. An important aspect of the data is that many rocks in the Bushveld Complex have conductivities that are one-third to one-half of the conductivity of strata in the Witwatersrand Basin and the overlying Transvaal Supergroup. This has important implications for understanding the geothermal gradients in the two regions.

Jones (2017) combined the borehole temperature data with the thermal conductivity data to make 31 new estimates of the heat flow through the Earth’s crust within the Bushveld Complex. Table III lists individual heat flow values including two results previously published by Carte and van Rooyen (1969), or a range of values if more than one measurement is available, for different segments of the platinum mining areas (Jones, 1988). Quartzites in the Witwatersrand Supergroup and dolomites in the Transvaal Supergroup have relatively high conductivities (6.4 and 5.1 W/mK respectively, Table V) and concomitantly low thermal gradients. The thermal gradient in Ventersdorp lava is higher than in the quartzites and dolomites because of its thermal conductivity, which can be written as:

\[
\frac{dT}{dx} = \frac{-q_0}{K}
\]

states that thermal gradient \((dT/dx)\) is inversely proportional to thermal conductivity \((K)\) and proportional to surface heat flow \((q_0)\) at a particular locality. In most cases, variation of thermal conductivity is the dominant cause of the differences in thermal gradient.

One exception is the thermal gradient in the Dominion Group (Table IV). The boreholes from which the average gradient in this unit was derived are situated on the northwest margin of the Witwatersrand Basin, where the heat flow is amongst the lowest in the world, 33±2 mW/m² (seven values) (Jones, 1988). Quartzites in the Witwatersrand Supergroup and dolomites in the Transvaal Supergroup have relatively high conductivities (6.4 and 5.1 W/mK respectively, Table V) and concomitantly low thermal gradients. The thermal gradient in Ventersdorp lava is higher than in the quartzites and dolomites because of its

---

Table III

Geothermal gradients and heat flow in platinum mining areas of the Bushveld Complex (after Jones, 2017)

<table>
<thead>
<tr>
<th>Area</th>
<th>(dT/dx) (K/km)</th>
<th>(q_0) [mW/m²]</th>
</tr>
</thead>
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<tr>
<td>Marikana</td>
<td>20.4–21.1</td>
<td>44–48</td>
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<tr>
<td>Rustenburg</td>
<td>19.3–24.5</td>
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<td>Steelpoort</td>
<td>21.2</td>
<td>43</td>
</tr>
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</table>

\(dT/dx\): thermal gradient; \(q_0\): heat flow

---

Table IV

Mean geothermal gradients in rock units above the main gold mining areas of the Witwatersrand Basin and platinum mining areas of the Bushveld Complex

<table>
<thead>
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<th>Rock unit/sub-unit</th>
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<td>KAROO SUPERGROUP</td>
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<td>BUSHVELD COMPLEX</td>
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<tr>
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<tr>
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<tr>
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<td>norite, anorthosite</td>
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<td>Lower Zone</td>
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<td>TRANSVAAL SUPERGROUP</td>
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<td>dolomite*</td>
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<td>lava*</td>
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</table>

\(dT/dx\): mean thermal gradient; s.d., standard deviation,*Jones (2003a); 
*Jones (1988)
Virgin rock temperatures and geothermal gradients in the Bushveld Complex

lower conductivity (3.4 W/mK, Table V). As discussed below, the high thermal gradients in rocks of the Bushveld Complex are associated with the low conductivities of the most predominant rock types. The highest thermal gradients occur in the young, low-conductivity rocks of the Karoo cover (Table IV).

The thermal gradients in Bushveld rocks and particularly those of the Main Zone, which overlie the main platinum mining horizons, are double those for rocks overlying the Witwatersrand goldfields. This means that the VRT must be higher. The difference in thermal regime is clearly illustrated in Figure 6, which compares actual VRT data from the Carletonville area (where the deepest gold mines in the Witwatersrand Basin occur) (Jones, 2003a) with VRT data from the Northam area (which hosts the deepest platinum mines in the Bushveld Complex) (Jones, 2017). The latter data-set comprises bottom-hole temperature measurements from deep exploration boreholes northeast of Northam, which were deemed to be too disturbed by underground water flow to yield reliable thermal gradients, as well as bottom-hole temperatures measured in boreholes listed under Northam and Amandelbult in Table II. The maximum VRT in the Northam area is 70°C at a depth of 2.2 km. This is 30°C hotter than gold mines at equivalent depths in the Carletonville area. As indicated in the previous two subsections, this difference cannot be attributed to an enhanced heat flux in the platinum mining areas, which is lower than that in the gold mining areas. The difference is due to both a higher surface temperature and steeper gradient at Northam compared with Carletonville. The extrapolated surface temperature at Northam is 21.6°C compared with 19.3°C for Carletonville. Table V

<table>
<thead>
<tr>
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<th>Rock type</th>
<th>K ± s.d. (W/mK)</th>
<th>Range (W/mK)</th>
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<td><strong>BUSHVELD COMPLEX</strong></td>
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K, mean thermal conductivity (except when referring to kelvin in units of measure); s.d., standard deviation; N_k, number of conductivity measurements.

Figure 6—Bottom-hole temperature data from boreholes north and northeast of Northam in the Bushveld Complex, and virgin rock temperature data from the Carletonville area (after Jones, 2003a) in the Witwatersrand Basin. The least-squares regression line for the Northam data is T=21.6±20.5x, and for the Carletonville area T=12.5±11.7x.
12.5°C at Carletonville. The reason for this is that Northam is at a lower elevation (1020 m asl) and latitude (24.95°S) compared with Carletonville (1630 m asl and 26.40°S). The steeper thermal gradient at Northam (20.5 K/km) compared with Carletonville (11.7 K/km) is largely due to the difference in the thermal conductivities of the rocks in these regions. The surface heat flow in the platinum mining areas \( (q_0) \) is tightly constrained at 45±4 mW/m². The surface temperature \( (T_0) \) depends on latitude and altitude, as shown in Figure 7. The local surface temperature lapse rate as a function of elevation is –7.4 K/km, and as a function of latitude –1.9 K per degree. It should be noted that the surface temperatures in these diagrams were obtained by upward extrapolation of temperature-depth data from the deeper linear sections of borehole temperature profiles. These temperatures are higher than mean annual air temperatures because the rainy period in the Bushveld is during the summer, when more heat is transferred into the ground. The thermal conductivities of all important rock types occurring in the Complex are well established, so estimates of thermal conductivity as a function of depth, \( K(x) \), can be made provided the stratigraphy is known. Evaluation of the integral then becomes trivial. However, it should be noted that this calculation assumes that the heat flux is linear and vertical. Local horizontal changes in thermal conductivity can lead to a significant component of lateral heat transfer, in which case two-dimensional modelling (Jones, 2003a) would be required. Also, heat may be transferred locally by underground flow of water, which can disturb VRTs appreciably. This is a major issue where mines occur along deep-seated fracture zones in the Earth’s crust. Northam is an example. Measured temperature profiles and derived thermal gradients in some boreholes in the Northam area (Figure 8) show clear disturbance by underground water flow, represented by spikes in the temperature profiles and temperature variations that cannot be related to variation of rock type. The temperature profiles in these boreholes are not suitable for establishing geothermal gradients, but their bottom-hole temperatures are useful for establishing the general VRT trend shown in Figure 7.

**Prediction of VRT**

Although there is no substitute for making VRT measurements, reasonably accurate predictions for new prospective mining areas can be made from the data presented in this paper. The mafic rocks of the Bushveld Complex contain very low concentrations of the heat-producing elements uranium, thorium and potassium (Jones, 2017), so the most simple expression for the temperature as a function of depth is given by the following equation:

\[
T(x) = T_0 + q_0 \int_0^x \frac{1}{K(x)} \, dx
\]

The surface heat flow in the platinum mining areas \( (q_0) \) is tightly constrained at 45±4 mW/m². The surface temperature \( (T_0) \) depends on latitude and altitude, as shown in Figure 7. The local surface temperature lapse rate as a function of elevation is –7.4 K/km, and as a function of latitude –1.9 K per degree. It should be noted that the surface temperatures in these diagrams were obtained by upward extrapolation of temperature-depth data from the deeper linear sections of borehole temperature profiles. These temperatures are higher than mean annual air temperatures because the rainy period in the Bushveld is during the summer, when more heat is transferred into the ground. The thermal conductivities of all important rock types occurring in the Complex are well established, so estimates of thermal conductivity as a function of depth, \( K(x) \), can be made provided the stratigraphy is known. Evaluation of the integral then becomes trivial. However, it should be noted that this calculation assumes that the heat flux is linear and vertical. Local horizontal changes in thermal conductivity can lead to a significant component of lateral heat transfer, in which case two-dimensional modelling (Jones, 2003a) would be required. Also, heat may be transferred locally by underground flow of water, which can disturb VRTs appreciably. This is a major issue where mines occur along deep-seated fracture zones in the Earth’s crust. Northam is an example. Measured temperature profiles and derived thermal gradients in some boreholes in the Northam area (Figure 8) show clear disturbance by underground water flow, represented by spikes in the temperature profiles and temperature variations that cannot be related to variation of rock type. The temperature profiles in these boreholes are not suitable for establishing geothermal gradients, but their bottom-hole temperatures are useful for establishing the general VRT trend shown in Figure 7.
Virgin rock temperatures and geothermal gradients in the Bushveld Complex

Conclusions
Geothermal gradients in the platinum mining areas of the Bushveld Complex (17.7–24.5 K/km) are approximately double the gradients in rocks overlying gold reefs of the Witwatersrand Basin (9.6–12.9 K/km). The geothermal heat flux in platinum mining areas of the Bushveld Complex (45 mW/m²) is lower than that in the Witwatersrand goldfields (51 mW/m²). The main reason for the high thermal gradients in the Complex is the relatively low thermal conductivity of the constituent rocks compared with the Witwatersrand Basin. The VRT in the Northam area, the deepest area investigated in the Bushveld Complex, is as much as 30°C higher than the VRT at equivalent depths in the Carletonville area, where the deepest gold mining takes place; this is due both to elevated surface temperatures and thermal gradients in the Bushveld. The Bushveld thermal gradient, heat flux and thermal conductivity database make it possible to predict the VRT in new potential mining areas for platinum and other ores that may be extracted from the Bushveld Complex.

Acknowledgements
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References


First-order exchange and spherical diffusion models of heap leaching in PhreeqC

by P.J. van Staden* and J. Petersen†

Synopsis
PhreeqC is an open-source software designed to perform geochemical calculations. It offers a feature for modelling heap leaching according to the most modern ‘dual-porosity’ visualization, as well as an extensive database of reactions and equilibria. It should represent an attractive option for modelling the chemical heap leaching of oxidized ores since it requires only the entering of the system characteristics and a minimal amount of coding to formulate kinetic expressions and output parameters.

Both first-order exchange and spherical diffusion PhreeqC models are compared to published short-term pulse-test data (over a few days) and model fits to it. The former is simpler and more convenient to use, but the latter yields more realistic results over longer periods of leaching. Comparisons are also made between PhreeqC and HeapSim, the latter having been acclaimed as the most comprehensive heap leaching model to date. The two models were found to yield very similar results. The observed differences are ascribed to subtle differences in the methods of allocation of reagents to competing reactions and in the methods of advancing the liquid phase between time steps.

PhreeqC is slower to execute than HeapSim, but is more rigorous with regard to solution speciation and permits spatial variation of physical and chemical kinetic parameters. The latter feature makes it well suited to the modelling of the effects of segregation and stratification, which have been identified by a number of authors as phenomena that impact on heap leaching performance but which have not yet been systematically studied.

Keywords
heap leaching, modelling, first-order exchange, spherical diffusion.

Introduction
Heap leaching is a means of extracting metal values, typically from low-grade resources as well as from those with a relatively short life of mine within which to recoup the capital investment. It is commonly practised in many parts of the world; locally in southern Africa a growing number of feasibility studies and actual projects have been based on heap leaching, two examples of which are cited by Tassell (2014, 2015). A major attraction of heap leaching, compared to agitated tank leaching or concentrate production, is the savings in capital and operating costs by elimination of the need for fine milling. Furthermore, the relatively simple process chain of mining, stacking, and circulation of leach solution between the ore pile and downstream recovery plant requires a relatively moderate capital investment. The most important compromise that these savings entail is a relatively slow rate of extraction, and hence the ultimate extent of extraction at which a heap can still be operated economically is relatively low. Due to the apparent simplicity of the process, the complex relationship between the various phenomena that govern the metallurgical performance and financial profitability of heap leaching came to be appreciated only some years after its first large-scale adoption and after some project failures. It was then recognized that it is important to accurately estimate heap leaching process and plant design specifications, to speedily diagnose the performance of operating heaps, and to identify the most suitable measures for correcting underperformance. It further became apparent that the ability to do so relies heavily on the adequate identification of the relevant ore characteristics and on a fundamental understanding of the metallurgical, geomechanical, and hydraulic processes underpinning the heap leaching process (Ghorbani, Franzidis, and Petersen, 2016).

Generic value of heap leach modelling
The large collection of published heap leach models of various levels of sophistication, as summarized by Dixon (2003), is evidence of the utility of models for the design and understanding of heap leaching. For example, models have been proposed for applications such as the design of heaps (Jansen and Taylor, 2002), for the characterization of observed heap leaching performance (Miller, 2003), and for providing advice on the means for improving heap leaching performance (Dixon and Petersen, 2003). Modelling can add significant value to laboratory test work results by deeper interpretation of the

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First-order exchange and spherical diffusion models of heap leaching in Phreeqc

Observations and by predicting performance under various scenarios of operating parameters that were not tested experimentally. As a diagnostic tool, modelling can be used to analyse actual heap-leaching data to determine the critical factors affecting performance, for example whether improvements are more likely to be achieved by a narrower dripper spacing or by more aggressive acid addition.

Status of heap leach modelling
The theoretical understanding of the underlying phenomena has been improving. The most authoritative model of heap leaching today is the dual-porosity concept whereby the solution inventory of a heap is considered to be mostly immobile. Mass transport of reagents and leached products occurs by diffusion to and from sparsely distributed mobile flow channels (Petersen and Dixon, 2007a). Physical evidence of this mechanism has been provided by the observation of preferential flow channels below drippers (Petersen and Dixon, op. cit.) and by direct observation of areas of higher and lower liquid saturation (Guzman, Scheffel, and Flaherty 2006), and also through PET-scan imaging of a column packed with ore under HL conditions (Petersen, 2016).

Value of a Phreeqc model
The task of coding and the associated need for scripting/compiling software can be barriers to the development of a heap leaching model by heap leaching investigators and consultants who are not routinely involved in software development. These constraints can be largely overcome by the availability of the Phreeqc open-source geochemical freeware, which offers dual-porosity column leaching calculations as a built-in feature for which only the parameters need to be provided. Furthermore, Phreeqc has a built-in database of equilibrium and kinetic parameters for a large collection of minerals and soluble species, thereby obviating the need for the user to find that data. The user only needs to expand and modify the built-in database if and as required (Parkhurst and Appelo, 1999), and the built-in features of the software ensure convergence and stability of the solution.

It would not be onerous to construct a heap leach model in Phreeqc that possesses different physical and reaction kinetic properties in different parts of the heap. In custom-coded software, like HeapSim (Petersen and Dixon, 2007a), which appears to be the most systematically developed and tested heap modelling tool to date, this would require re-coding and recompilation. Apart from the generic applications listed above, this capability would make a Phreeqc model also useful for a study of the effects of segregation and stratification in a heap. Segregation/stratification is widely recognized as an inevitable consequence of current stacking practice, as is evident from Kappes (2002), Gross and Gomer (1992), Kerr (1997), Miller (1998), O’Kane, Barbour, and Haug, (1999), Smith (2002), and O’Kane Consultants (n.d.). However, only O’Kane, Barbour, and Haug (1999) and Wu et al. (2007) have published results of studies on the phenomenon, while Miller (1998) refers to related observations made during chloride tracer tests, leaving segregation/stratification as a topic still worthy of further study.

Need for model calibration
To provide confidence in the model outputs, and to be able to place the model results in context relative to results of earlier published models, it is necessary to compare and calibrate the results of the Phreeqc model with results produced by other models for the same input parameters. In particular, it is necessary to compare the Phreeqc model results with those of other dual-porosity models, because this is the most modern heap leach model concept, as mentioned above. Two such comparisons have been made possible by the availability of pulse-test data published by Bouffard and Dixon (2001), and by the availability of the HeapSim software to the authors. A comparison with HeapSim is of particular importance since it is a very comprehensive model, more so than any other model presented in the public domain (Dixon, 2003). Furthermore, case studies have been based on that software, including those by Dixon and Petersen (2003) and Petersen and Dixon (2007b), so that it has become a benchmark.

The built-in data blocks of Phreeqc allow two generic types of dual-porosity models to be constructed, namely a ‘first-order exchange’ model and a ‘diffusional’ model. Both of these Phreeqc models are calibrated to the earlier models to optimize the correlations between the results of the various models for a given set of inputs. This renders the Phreeqc modelling results directly comparable with the published results obtained on the earlier models. In the process, similarities and differences, and advantages and disadvantages of the various models can be explored.

Background

Terminology
Various authors inevitably use different terminology, which can cause confusion. Phreeqc terminology is used as the standard in this paper. The names of Phreeqc data blocks, such as MIX and TRANSPORT, appear in capitals throughout this text, because that is the form in which they appear in the software and in the user manual by Parkhurst and Appelo (1999). Other important terms to note are:

- The descriptors ‘mobile’ and ‘immobile’ in Phreeqc are termed ‘flowing’ and ‘stagnant’ by Bouffard and Dixon (2001)
- What is termed in this text the ‘first-order exchange’ model equates to the ‘mixed side-pore diffusion (MSPD)’ model of Bouffard and Dixon (2001)
- What is termed in this text the ‘diffusional’ model equates to the ‘profiled side-pore diffusion’ (PSDP) model by Bouffard and Dixon (2001), and is similar to that described by the ‘Turner structure’ underlying the HeapSim model. A difference is that Phreeqc uses a ‘tank-in-series’ description of flow between finite solution increments compared to the plug-flow approach of HeapSim.
- The spatial increments used for finite difference description of a heap are termed cells in Phreeqc. The term shift refers in Phreeqc to the advance of the solution inventory of a single mobile cell to the next mobile cell below it. These unique Phreeqc terms will be shown in bold throughout the text.
First-order exchange and spherical diffusion models of heap leaching in PhreeqC

Measures of concentration
The default units in which solution concentrations are specified in PhreeqC are gmole per kg water (i.e., molar concentration). Because heap leaching involves only relatively low-concentration solutions, unit conversions where required for the calculations shown in this text have equated the 1 kg water basis to 1 litre of solution and molar concentrations to molar concentrations, and all solutions were assumed to possess a density of 1 kg/L. A trial calculation in PhreeqC showed that a 10 g/L Cu2+ (0.157 molar Cu2+) solution in 10 g/L H2SO4 medium has a molality where required for the calculations shown in this text have equated the 1 kg water basis to 1 litre of solution and molar concentrations to molar concentrations, and all solutions were assumed to possess a density of 1 kg/L. A trial calculation in PhreeqC showed that a 10 g/L Cu2+ (0.157 molar Cu2+) solution in 10 g/L H2SO4 medium has a molality of 0.159 mole Cu2+ per kg water. Therefore if the 0.159 molal molar Cu2+ solution in 10 g/L H2SO4 medium has a molality of 0.159 mole Cu2+ per kg water. Therefore if the 0.159 molal molar Cu2+ result is treated as if it means 0.159 molar Cu2+, the amount of Cu2+ is overestimated by about (0.159/0.157 - 1), or 1.3%.

Geometry
The diffusional versions of the Bouffard and Dixon and PhreeqC models, as well as the HeapSim model, can consider either linear, cylindrical, or spherical geometry for the immobile space in which diffusion occurs. However, Bouffard and Dixon published only the data generated for spherical geometry, against which the calibration described in this text could be performed. Therefore, only spherical diffusion versions of HeapSim and PhreeqC are considered in this text.

Formulations of the dual-porosity leaching mechanism
First-order exchange model
Bouffard and Dixon (2001) proposed two models for the study of the hydrodynamics of solution passing through a packed bed of solids. The first-order exchange visualization shown in Figure 1a divides the mobile pathway into a stack of vertical cells, and associates each mobile cell with a single cell filled with solution that is immobile but of uniform concentration. The governing equation is:

\[ \theta_{imm} \frac{\partial C_{i,imm}}{\partial t} = \alpha (C_{i,mob} - C_{i,imm}) \]  

where \( C_{i,mob} \) and \( C_{i,imm} \) are the solute concentrations (normalized with respect to input and background concentrations) of species \( i \) in the aqueous phases contained in the mobile and immobile cells respectively. The term \( \theta_{imm} \) is the volume fraction of the ore bed occupied by immobile solution, \( \alpha \) is an exchange coefficient, and \( t \) represents time.

Spherical diffusional visualization
In the diffusional model, illustrated in Figure 1b, each mobile cell is associated with pores filled with immobile solution, and dissolved species move to and from the mobile solution by diffusion, driven by concentration profiles in the immobile pores. Bouffard and Dixon (2001) also proposed another version of this model with a non-uniform pore length distribution: in this text only their results obtained with the model with pores of uniform length are considered.

The Turner structure upon which the HeapSim model is based is illustrated in Figure 2a, consisting of a flowing stream of mobile solution from which stagnant diffusional pathways extend into the surrounding ore bed. Figure 2b illustrates how this relates to spherical parcels of ore being irrigated by drippers from above.

Both visualizations of leaching with diffusional mass transfer via an immobile zone are mathematically described by Fick’s second law of (transient) diffusion. In the absence of chemical reaction and for the case where the side-pores extend into a spherical space of radius \( R \), the transport of chemical species through the stagnant region is, according to the conventions of van Genuchten (1985) and Dixon (2003), described by:

\[ \frac{\partial C_{i,imm}}{\partial t} = D_e \left[ \frac{\partial^2 C_{i,imm}}{\partial r^2} + \frac{1}{r} \frac{\partial C_{i,imm}}{\partial r} \right] \]  

with boundary conditions:

\[ C_{i,imm}(r,0) = 0; \quad C_{i,imm}(R,t) = C_{i,mob} \quad \text{and} \quad \frac{\partial C_{i,imm}}{\partial r} \bigg|_{r=0} = 0 \]

where \( r \) is the radial position and \( D_e \) is the effective diffusivity of the ore bed matrix. The term \( D_e \) deviates from the free diffusivity \( D \) by a factor determined by the tortuosity and restrictivity of the pores containing the immobile solution, which are expected to render \( D_e < D \).

Both the HeapSim and PhreeqC diffusion models utilize Equation [2] to describe diffusion in spherical immobile zones. In the footnote to Table I, comment is provided on the slightly different convention used by Bouffard and Dixon (2001) to formulate their equivalent of Equation [2].

Relationship between the first-order exchange and diffusional models
Although the first-order exchange model of Equation [1] is simpler to use than the diffusional model of Equation [2], the exchange coefficient in Equation [1] does not have clear physical significance. To help overcome this shortcoming,

![Image](81x296 to 243x323)

![Image](345x463 to 512x493)

Figure 1—The dual-porosity concepts as visualized by Bouffard and Dixon (2001): (a) first-order, and (b) diffusional
van Genuchten (1985) provided shape factors whereby the exchange coefficient of the first-order exchange model is correlated to the characteristic dimensions of various immobile-zone geometries. According to van Genuchten’s relationship for conversion between spherical geometry and the first-order exchange formulation:

$$\frac{\alpha}{\theta_{imm}} = \frac{D_e}{R^2 \left(f_{s,1}\right)^2}$$

where $f_{s,1}$ is a shape factor with value 0.21. However van Genuchten (1985) pointed out that for spherical geometries the conversion did not yield very accurate results, and the same conclusion is drawn in the discussion of the trends observed in Figure 5 below.

### The models in terms of diffusion time

Estimating overall effective in situ values for the tortuosity and restrictivity of diffusional channels in a heap with the aim of obtaining an accurate value of $D_e$ is quite impractical. Estimating the value of $D_e$ is further complicated by the fact that any lateral capillary flow will increase the value of $D_e$, potentially even rendering it larger than the free diffusivity $D$, as has been confirmed by Dixon (2003).

Estimating the value of $R$ as about half of dripper spacing on a heap, or half of the diameter of a laboratory column, has been suggested by Dixon and Petersen (2003). That can be visualized as per Figure 2b. However there is not yet sufficient field data available to predict, with any known level of statistical confidence, the likelihood of flow channels below drippers splitting or merging. In fact, the visualization

---

**Table I**

<table>
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<th>Parameter</th>
<th>Bouffard and Dixon, 1st-order exchange</th>
<th>Bouffard and Dixon, spherical diffusion</th>
<th>PhreeqC 1st-order exchange</th>
<th>PhreeqC spherical diffusion</th>
<th>HeapSim spherical diffusion</th>
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<td></td>
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</tr>
<tr>
<td>Immobile (stagnant) voidage $\theta_{imm}$</td>
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<td>0.22 (exp)</td>
<td>0.1116 (fit)</td>
<td>0.1088</td>
<td>0.0916</td>
</tr>
<tr>
<td>Exchange coefficient $\alpha$ (s⁻¹)</td>
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<td>1.06×10⁻⁵</td>
<td>1.06×10⁻⁵</td>
<td>1.73×10⁻⁵</td>
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<tr>
<td>Diffusivity ($D$, $D_e$), m²/s</td>
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<td>Not applicable</td>
<td>1.40×10⁻⁸</td>
<td>1.47×10⁻⁸</td>
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<tr>
<td>$\sqrt{\Gamma/\Gamma_{ref}}$</td>
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<td>1.0 (Reference)</td>
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<td>0.947</td>
<td>0.992</td>
</tr>
</tbody>
</table>

---

(1) As per the discussion in section II-B of Bouffard and Dixon (2001), they use a different set of units for $D$ to that employed for $D_e$ in Equation [2] above, to yield, according to their equation [5] for a spherical space: $\frac{\partial^2 C_{imm}}{\partial t^2} = \frac{D}{\theta_{imm}} \left( \frac{\partial^2 C_{imm}}{\partial r^2} + \frac{2 \partial C_{imm}}{r \partial r} \right)$, therefore the diffusion time in this case is calculated as $\Gamma = \theta_{imm} \frac{r^2}{D_e}$. 

---

Figure 2 – Dual-porosity concept: (a) Turner structure used for HeapSim, (b) illustration of flow channels around spheres with radius of half dripper spacing, (c) visualization offered by Petersen and Dixon (2003)
First-order exchange and spherical diffusion models of heap leaching in PhreeqC

given to dual-porosity hydrodynamics offered by Petersen and Dixon (2003) shows indeed flow channels splitting and merging as per Figure 2c, and this seems to be supported by PET imaging of flow through a column (Petersen, 2016). Furthermore Dixon and Afewu (2011) have calculated the anticipated size of the plume of flowing solution developing below a dripper under the influence of capillary forces. According to their calculations, the advective plume below a dripper occupies a volume typically some hundreds of millimetres in diameter, even under conditions of slow irrigation or within a poorly dispersing ore mass. In such a scenario, the spaces occupied by stationary solution between flow channels must be smaller than half of the dripper spacing, which is also of the order of some hundreds of millimetres.

It is therefore preferred to reformulate Equation [2] into a form that avoids the need to ascribe definitive values to either \( D_0 \) or \( R \). This is achieved, analogous to Dixon (2003), by defining the dimensionless radial position (\( \xi \)) as

\[
\xi = \frac{r}{R}
\]

so that \( 0 \leq \xi \leq 1 \)

and the ‘diffusion time’ (\( \Gamma \)), with units of seconds, as:

\[
\Gamma = \frac{R^2}{D_e}
\]

By substitution of the expression for \( \Gamma \) in Equation [4a] into Equation [5], it follows that the diffusion time for the first-order exchange case can also be written as:

\[
\Gamma = \frac{\theta_{imm}}{\alpha} \left( \frac{f_{s,1}}{T_s} \right)^2
\]

For the case of spherical diffusion, Equation [2] can hence be re-written as:

\[
\frac{\partial C_{i,imm}}{\partial t} = \frac{1}{\Gamma} \left[ \frac{\partial^2 C_{i,imm}}{\partial \xi^2} + \xi \frac{2 \partial C_{i,imm}}{\partial \xi} \right]
\]

The diffusion time therefore provides a single number for comparison of the hydrodynamics of systems being modelled by the first-order and diffusional models.

Although the concept of diffusion time now eliminates the direct comparison (in units of length) of the diffusional path lengths of different cases, one diffusional path length (\( R \)) can still be expressed as a fraction of the diffusional path length of a reference case \( (R_{ref}) \) if the two cases are assumed to exhibit the same diffusivity (even though the magnitude of the diffusivity may be unknown) by noting in such a case that:

\[
\sqrt{\frac{\Gamma}{\Gamma_{ref}}} = \frac{R}{R_{ref}} \Rightarrow D_e \text{ is invariant}
\]

**PhreeqC model set-up**

**Stability criteria**

A feature of the ADVECTION and TRANSPORT data blocks of PhreeqC for simulating percolation through permeable columns is that they are based on the displacement of one mobile cell volume per shift. Accuracy and stability of advection flow calculations in PhreeqC therefore require that the following relationship should always be maintained, according to Parkhurst and Appelo (1999):

\[
\Delta t = \frac{\Delta z}{v} = \frac{\Delta t_{mob}}{u}
\]

where \( \Delta z \) is the vertical increment length, \( \Delta t \) is the time increment, \( v \) is the velocity of flow through the mobile zone, and \( u \) is the superficial velocity calculated as the flux per unit area of empty column. This means that \( \Delta z \) and \( \Delta t \) cannot be selected independently by the user: selecting one determines the other.

As will be discussed further below, MIX factors are used in PhreeqC to emulate diffusion in the immobile zone. In doing so, it is a requirement that the amount of solution mixed from any cell to any of its neighbours should always be less than \( \frac{1}{3} \) (conversely, after mixing each cell should retain at least \( \frac{1}{3} \) of its content from the previous time step).

This condition can be met by the appropriate selection of \( \Delta z \).

**Fineness of grid**

The PhreeqC calibration was conducted with the number of mobile cells varied from 20 to 75, with very little difference between results and hence only results using 20 and 40 mobile cells are shown. During subsequent optimization, this was further reduced to 10 mobile cells with virtually no noticeable variation in the quality of the results. Because the HeapSim input interface suggests a minimum of four immobile cells per mobile cell, the number of immobile cells in both HeapSim and the PhreeqC diffusional model was kept constant at five per mobile cell for the results presented here. It was verified that PhreeqC diffusional model calculations using four, five, or six immobile cells per mobile cell yielded results that were virtually indistinguishable.

The mobile solution fraction \( \theta_{mob} \) does not feature in the continuity equation of the diffusional models, as can be seen from Equations [2] and [5], and this applies to both PhreeqC and HeapSim. Therefore parameter \( \theta_{mob} \) can be varied arbitrarily in order to indirectly adjust the time step \( \Delta t \) (as a result of Equation [7]) as large as possible to enhance computational speed, provided it is done within the stability constraint for the MIX factors which are affected by the magnitude of \( \Delta t \) in accordance with Equation [8].

**PhreeqC finite difference formulation**

The TRANSPORT data block of PhreeqC allows the specification of the number of mobile cells \( N_s \) into which the column is divided vertically. Each mobile cell can be associated with either a single immobile cell (to yield a first-order exchange dual-porosity model) or any number \( N_i > 1 \) of immobile cells per mobile cell to yield a diffusional dual porosity model.

For the first-order exchange case \( (N_i = 1) \) the user needs only to specify the parameters that appear in Table I as inputs. However, to simulate diffusion mass transfer between multiple immobile cells per mobile cell \( (N_i > 1) \), the user is required to specify the ratios according to which bordering immobile cells exchange soluble species during each time step. This is a powerful feature since it permits any type of 3D geometry of the immobile cells to be simulated, including...
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the case where immobile cells may be exchanging solutions between different vertical layers. In contrast, HeapSim provides for only linear, cylindrical, or spherical immobile geometries, and diffusion between bordering layers vertically is not allowed. Parkhurst and Appelo (1999) illustrated the equivalence between diffusion mass transfer in the immobile zone and the operation of the MIX data blocks of PhreeqC. Their derivation is briefly repeated here with reference to the concept of a mobile cell containing a spherical immobile zone consisting of five concentric spheres, as illustrated in Figure 3. It is expressed in generic terms of volume \( V_j \) and contact areas \( (A_k) \) so that it remains applicable to any immobile geometry. Consider the accumulation of a solute in cell \( j \) between time \( t1 \) and \( t2 \) (with \( t2 = t1 + \Delta t \), the latter term being the time increment) with cell \( j \) being positioned between cells \( i \) and \( k \) (with \( i \) being the stagnant cell to the outside of \( j \), closer to the mobile cell, and cell number \( k \) is the cell on the inside towards the centre of cell \( j \)). That is to consider only diffusion laterally between cells associated with a single mobile cell, without any exchange vertically between immobile cells, although PhreeqC can accommodate vertical exchange as well. By writing Equation [2] in terms of finite differences for any geometry it follows that:

\[
\theta_{imm} V_j C_j^{t2} = \theta_{imm} V_j C_j^{t1} + \theta_{imm} D_e A_{k,j} \frac{(C_k - C_j)}{h_{j,k}} \Delta t + \theta_{imm} D_e A_{k,j} \frac{(C_i - C_j)}{h_{i,j}} \Delta t + 1000 \dot{X}_j C_j^0 \theta_{imm} V_j \Delta t
\]

which can be re-arranged after elimination of \( \theta_{imm} \) to yield:

\[
C_j^{t2} = \left(1 - \sum_{n \neq j} M_{X_{n,j}}\right) C_j^{t1} + \sum_{n \neq j} M_{X_{n,j}} C_n^{t1} + 1000 \dot{X}_j C_j^0 \Delta t
\]

where

\[
M_{X_{n,j}} = \frac{D_e A_{n,j} \Delta t}{V_j h_{n,j}}
\]

represents the proportion of the content of cell \( n \) that is mixed into cell \( j \), where \( n \) can be on either side of \( j \). \( V_j \) is the volume of cell \( j \), \( A_{n,j} \) is the contact area between cells \( n \) and \( j \), and \( h_{n,j} \) is the distance between the mid-points of cells \( n \) and \( j \). The mixing factor from cell number \( n \) into cell number \( j \) is written (for the general case that includes the mobile cell):

\[
MX_{n,j} = \frac{D_e A_{n,j} \Delta t}{V_j h_{n,j}} f_{bc} \forall n \neq j \text{ and } \forall r < R
\]

The mixing factor of the central cell \( j \) to itself (i.e. the proportion of cell contents of cell \( j \) that remains behind after mixing to neighbouring cells), namely \( MX_{jj} \), is the difference between unity and the sum of the mixing factors to neighbouring cells. Example 13c of the PhreeqC software provides a working model of this type. The term \( f_{bc} \) is unity for the mixing between all cells inside the spherical immobile zone. For mixing at the boundary between mobile and immobile solution, the diffusional distance \( h_{n,j} \) extends from the centre of the outer spherical cell to the boundary wall (opposed to centre-to-centre for diffusion between elements inside the sphere), i.e. \( h_{n,j}/2 \) so that:

\[
f_{bc} = 2 \text{ for } r = R
\]

for calculating the mixing factor between the outer cell of the sphere and the mobile cell. It is not simple to eliminate \( D_e \) and the terms bearing \( R \) (namely \( A_{j,k} \), \( V_j \) and \( h_{n,j} \)) from Equation [8] to express it in terms of diffusion time, since the expression needs to provide for the contact area \( A_{j,k} \) on both the inside and outside of spherical shell element \( j \), which prevents terms from being cancelled conveniently. A pragmatic approach for simulating a system with a given diffusion time in PhreeqC or HeapSim would be to adopt any arbitrary value for \( D_e \) (or set it equal to \( D_i \)), and then to calculate the value for \( R \) that is required to obtain the desired diffusion time from the expression provided for \( r \) in Equation [4a].

**Kinetic expressions**

For the purpose of comparing the PhreeqC and HeapSim simulation results for the cases that included chemical reaction, the kinetic expressions built into HeapSim were used. The rate of conversion of copper oxide \((\dot{X}_{CuO})\) is described by:

\[
\dot{X}_{CuO} = K_{CuO} C_{acid} (1 - X)^{\phi}
\]

where \( K_{CuO} \) is the reaction rate constant, \( C_{acid} \) is the molar acid concentration, \( X \) is the extent of conversion, and \( \phi \) is the exponent on the unreacted fraction. Similarly, the expression used to describe the rate at which acid is consumed by gangue (gange acid consumption; GAC) is:

\[
\dot{X}_{acid} = K_{GAC} C_{acid}
\]

with \( K_{GAC} \) being the GAC rate constant.

**Mass balance calculations**

The HeapSim model performs all calculations, including mass balancing and graphing of the results, and therefore does not require any further calculations from the user. However, PhreeqC provides as outputs essentially the solution...
First-order exchange and spherical diffusion models of heap leaching in PhreeqC

compositions, and it is left to the user to calculate from those outputs information such as the percentage extraction and reagent consumption.

PhreeqC requires the solid phase content to be specified in terms of a concentration in the water content of the immobile cells. The initial molal concentration of solid species $i$ in the solution of any immobile cell is specified as:

$$ C_{i,imm}^0 = \frac{\rho W_i^0}{M_i \theta_{imm}} $$

[12]

where $\rho$ [kg/m$^3$] is the solid phase bulk density (dry basis), $W_i^0$ is the mass fraction of species $i$ in the solid phase at time zero, $M_i$ is the molar mass of species $i$, and $\theta_{imm}$ is the volume fraction of immobile solution in the ore bed. The extent of extraction of a solid-phase element $i$ to the drainage solution, $X_{i,\text{drain}}$, is calculated as the following summation over the number of shifts that have taken place:

$$ X_{i,\text{drain}} = \sum_{\text{shifts}} \theta_{i,\text{mob}} \frac{C_{i,\text{drain}}}{\theta_{imm} C_{i,\text{imm}}^0} N_z $$

[13]

$$ = \sum_{\text{shifts}} \frac{C_{i,\text{drain}}}{\theta_{imm} C_{i,\text{imm}}^0} \frac{u \Delta t}{N_z \Delta z} $$

The second and last terms follow by substitution first with Equation [7] and then with Equation [12]. Here, $C_{i,\text{drain}}$ is the concentration in the drainage solution, $N_z$ is the number of vertical increments, and $C_{i,\text{imm}}^0$ is defined by Equation [12].

In the case of the first-order exchange model, the overall extent of conversion of the solid-phase element $i$ equals the following summation over all immobile cells:

$$ X_i = 1 - \sum_{n=1}^{N_z} \frac{C_{i,n}}{N_z C_{i,\text{imm}}^0} $$

[14]

where $C_{i,n}$ is the concentration of solid species $i$ remaining undissolved in the solution of immobile cell number $n$, $C_{i,\text{imm}}^0$ is defined by Equation [12] and $N_z$ is the number of mobile cells (i.e., number of vertical increments which equals the number of immobile cells in the case of the first-order model). This quantity is always slightly higher than the extent of extraction to the drainage solution, by the amount of element $i$ that has been solubilized but has not yet exited the column.

In the case of the spherical diffusion model, the fact that different spherical increments contain different volumes of immobile solution is corrected for by the MIX factors in Equation [8] to calculate the concentration of species in solution correctly. To calculate the extent of extraction from the solid phase, the amount of solid phase allocated to cell number $n$ away from the mobile zone is factored in accordance with spherical geometry, by the factor $P_n$:

$$ P_n = \frac{3}{4} \pi \frac{r_{n,\text{out}}^3 - r_{n,\text{in}}^3}{R^3} $$

[15]

where $r_{n,\text{out}} = R - n \cdot h + h$ and $r_{n,\text{in}} = R - n \cdot h$, $0 \leq n \leq N_z$

with $N_z$ being the number of immobile cells associated with each mobile cell (i.e., the number of concentric spherical increments whereby the immobile phase is represented) and $R$ and $r$ being respectively the radius of the stagnant zone and the distance along the radius.

Therefore, for the spherical diffusion model, the overall average extent of extraction of the solid-phase element $i$ equals the following summation over all immobile cells:

$$ X_i = 1 - \sum_{n=1}^{N_z} \frac{C_{i,n}}{N_z C_{i,\text{imm}}^0} $$

[16]

where $C_{i,n}$ is the concentration of element $i$ remaining in the solid phase contained in the solution of immobile cell number $n$, $C_{i,\text{imm}}^0$ is defined by Equation [12], and $N_z$ is the number of mobile cells (i.e., number of vertical increments).

The total acid consumption (TAC, [kg/t]) is determined from:

$$ TAC = CA \cdot X_i \cdot \rho \cdot N_z $$

[17]

where $CA$ [kg/t] represents the curing acid addition. For the case where copper is being extracted from an oxide copper mineral such as malachite, one mole of acid is consumed for each mole of copper extracted. Therefore, in that case the acid consumption ascribed to the reaction of gangue constituents, $GAC$ [kg/t], is calculated as the difference between $TAC$ and the moles of copper extracted thus:

$$ GAC = TAC - 1000 \frac{M_{\text{acid}}}{M_{\text{Cu}}} $$

Integration with Excel

The calculation of MIX factors was automated in Excel, and these were integrated with the Phreeqc data block statements, also in Excel. Charlton and Parkhurst (2011) provided VBA code for an Excel macro whereby Phreeqc statements contained on an Excel sheet can be passed to Phreeqc and the results printed in another Excel sheet, using the Phreeqc Component Object Module (COM). This also facilitates the use of the features of Excel for the mass balance calculations, graphing of results, and statistical analyses. HeapSim offers the same features by operating from an Excel interface, with a predefined set of graphing features as standard.

Methodology

Short-duration pulse test (no chemical reaction)

Bouffard and Dixon (2001) provided operating parameters as well as the resulting concentration curves (normalized with respect to the background and inlet concentrations) for tracer tests conducted in 1.6 m high columns of ore, using 3.4 M NaNO$_3$ as inlet pulse against a background of 0.05 M NaNO$_3$. The pulses had essentially passed through the columns in 4 to 5 days. They optimized the fit of their first-order exchange model to the pulse test data by using the immobile porosity and exchange coefficient as independent variables in a multi-dimensional optimization routine, minimizing the sum of squared residuals (SSR) between experimental and model
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data. The diffusional model was fitted using the immobile porosity and diffusional path length \((R)\) as fitting factors. They also determined the mobile and immobile porosities experimentally as a comparison with the fitted values.

This published experiment was simulated in a PhreeqC first-order exchange model, in a PhreeqC diffusional model, and in HeapSim. Firstly, the published parameters that yielded the best fits between the Bouffard and Dixon model and the experimental data were used as inputs to the PhreeqC and HeapSim models. Secondly, the immobile porosity and exchange coefficient (for the PhreeqC first-order exchange model) and immobile porosity and diffusional path length (for the PhreeqC diffusional model and for HeapSim) were used as independent variables in a two-dimensional Newtonian optimization routine. Optimization was achieved by minimizing the SSR between the model result and experimental pulse test data. One deviation was that the HeapSim model was set up using copper as tracer element instead of sodium, because HeapSim does not provide for sodium as a chemical species; however, the name attached to the chemical species in the model has no bearing on the modelling results: they rely on the diffusivity being correctly specified.

As discussed before, diffusion time is proposed as a comparative characteristic instead of diffusivity and diffusional path length. However, the models still require inputs in terms of the latter two variables in order to effect a required diffusion time, with the understanding that these two variables are not independent but that their ratio contributes towards the diffusion time. Therefore in Table I the diffusivity and diffusional path lengths used in the model calibrations are shown, together with the diffusion time calculated from them.

Long-term leach curve (with chemical reaction)
The batch curves for copper leaching and for GAC were derived from the production and cumulative acid consumption curves of Panel 1 of the Tschudi oxide copper heap leach operation in Namibia, using the methodology proposed by van Staden et al. (2017). This methodology is required where the operational data is generated with irrigation and copper production being initiated while stacking continues. In such a case the production data is generated from ore portions with a range of different leaching ages, so that the rate of copper production \(versus\) time does not represent a batch extraction curve. The methodology derives the shape of the batch curve from the copper production data in the form of an exponential decay of the unleached mineral.

The copper extraction and GAC outputs of the HeapSim model were then fitted to the Tschudi batch curves thus determined by manipulation of the input variables to HeapSim. The heap height, bulk density, copper content (represented in this text by the hypothetical mineral CuO), irrigation rate, and acid concentration in the irrigation solution were known from data records. A volumetric irrigation rate, and acid concentration in the irrigation (represented in this text by the hypothetical mineral CuO), HeapSim determined by manipulation of the input variables to model were then fitted to the Tschudi batch curves thus derives the shape of the batch curve from the copper extraction and acid consumption curves in opposite ways, (b) changes to the power of the unconverted fraction affect both the diffusion of the initial linear rate of copper leaching and copper leach rate, without affecting the GAC, and (c) changes to the diffusion path length affect the rate of copper extraction and GAC in similar ways. Finally, the diffusional path length \(versus\) diffusion time) was further optimized by a one-dimensional search over a range that included the minimum SSR, keeping the near-optimized values of the other variables fixed.

The HeapSim leach curve thus fitted appears in Figure 7 as the data-set labelled ‘HeapSim (KGO=0.037)’ and served as the reference curve against which both first-order exchange and diffusional versions of the PhreeqC model were calibrated. Arbitrarily selected variations were applied to the GAC rate constant and to the curing acid addition to expand the range of input parameters used for calibration. The diffusional path length \(versus\) diffusion time) was used as the sole independent variable manipulated to optimize the data fits between the PhreeqC models and the HeapSim reference curve, using zero GAC and zero curing acid addition. Subsequent comparisons between the models with GAC and with curing acid were performed using the optimized diffusional path length \(versus\) diffusion time) with no further parameter fitting.

Correlation coefficients
The value of the Pearson’s correlation coefficient \((\text{Cor}.)\) (which is calculated by the \(\text{Correl}\) function of Excel), is indicated in the legends of the graphs appearing in this text with the prefix ‘\(\text{Cor.}=.\)’, to indicate the goodness of fit between data-sets. The reference data set is indicated by ‘Ref.’ For example, in Figure 4a the experimental data labelled ‘Data Test 10b (Ref.)’ serves as the reference. The correlation coefficient between the data generated by the first-order exchange version of the PhreeqC model with 40 vertical increments \((\text{i.e. with } 40\times1\text{ vertical by lateral spatial segmentation})\) and the experimental data is 0.949. This is indicated in the legend of Figure 4a for the results of that PhreeqC model version as ‘\(\text{PhreeqC } 40\times1 \text{ Cor.}=.949\)’. The experimental data is indicated by diamond markers while the PhreeqC 40x1 calculations appear as circular markers on the same graph, illustrating the fit visually.

Results and discussion
Hydrodynamic responses during pulse test simulations
The data published by Bouffard and Dixon (2001) appears in Table I, together with the parameters used and results obtained when their pulse test experiment was emulated in PhreeqC and in HeapSim. The response curves, showing normalized drainage concentration \(versus\) time, appear in Figure 4, from which it can be seen that the curves produced by the PhreeqC diffusion model and by HeapSim (Figure 4b) were generally more closely spaced than the PhreeqC first-order exchange results (Figure 4a). None of the three models considered here reproduced the top of the peak observed in
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The response curve; that was achieved only by a model used by Bouffard and Dixon, which utilized a diffusional path length distribution (as opposed to a single uniform length). That is a feature that has not been included in the PhreeqC models presented here and it is not provided for in HeapSim. Hence no comparison of models with that feature is possible and the results from the Bouffard and Dixon model with the path length distribution are therefore not presented or discussed here.

From Table I it can be seen that the diffusional path lengths (and diffusion times) that yielded the best fits were somewhat lower than those reported by Bouffard and Dixon for both the PhreeqC and HeapSim models, with the HeapSim path length and diffusion time being the shortest. A little more information about the formulation of the model by Bouffard and Dixon, which was coded in MS Fortran Visual Workbench, is provided in the footnote to Table I.

PhreeqC models with leaching reaction, zero GAC

The PhreeqC first-order exchange model was compared with HeapSim for the simulation of the Tschudi heap leach data, using the parameters that appear in Table II and zero GAC. From the graphs and correlation coefficients appearing in Figure 5, the fits can be seen to be quite crude. The inclusion of GAC and curing acid addition in the first-order exchange simulations yielded similar fits and is not reproduced here.

The PhreeqC diffusional model reproduced the trends calculated by HeapSim much better than the PhreeqC first-order exchange model, as evidenced by Figure 6. Both diffusional and first-order exchange versions of PhreeqC attempt to emulate the effect of diffusion from a mobile cell into a lateral channel filled with immobile solution. The first-order exchange model attempts to achieve this in a simplified manner, using a mass transfer coefficient between each mobile cell and its single accompanying immobile cell. During short-lived scenarios of the type appearing in Figure 4, the concentrations in a mobile cell and the single accompanying immobile cell of the first-order exchange model equalize more readily. It then becomes impossible to accurately emulate the lateral plug-flow of HeapSim (or the lateral tanks-in-series flow of the PhreeqC diffusional model) by tuning of the mass transfer coefficient of the first-order exchange version of the PhreeqC model.

The optimal correlation between HeapSim and the diffusional version of the PhreeqC model was obtained by

---

Table II

<table>
<thead>
<tr>
<th>Parameter and units</th>
<th>Value</th>
</tr>
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<tbody>
<tr>
<td>Heap height, m</td>
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</tr>
<tr>
<td>Bulk density, kg/m^3 (dry basis)</td>
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</tr>
<tr>
<td>CuO content (as % Cu)</td>
<td>0.44</td>
</tr>
<tr>
<td>Irrigation rate, L/(h.m2)</td>
<td>5.38</td>
</tr>
<tr>
<td>Irrigation (H_2SO_4) g-mole/L</td>
<td>0.11 (pH = 0.96)^(1)</td>
</tr>
<tr>
<td>Rate constant CuO dissolution, h^-1</td>
<td>0.3</td>
</tr>
<tr>
<td>Power of unreacted fraction, (dimensionless)</td>
<td>1.0</td>
</tr>
<tr>
<td>Rate constant of gangue acid dissolution, 1/h</td>
<td>0 or 0.037 or 0.10</td>
</tr>
<tr>
<td>Curing acid addition, kg/t</td>
<td>0 or 7</td>
</tr>
<tr>
<td>Number of mobile cells (vertical increments)</td>
<td>20</td>
</tr>
<tr>
<td>Number of immobile cells per mobile cell (spherical increments)</td>
<td>1 for 1st-order exchange</td>
</tr>
<tr>
<td>Volumetric immobile moisture fraction, (-)</td>
<td>5 for spherical diffusion</td>
</tr>
<tr>
<td>Diffusion time, days</td>
<td>0.1</td>
</tr>
<tr>
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<td>70.4</td>
</tr>
</tbody>
</table>

---

1Trial simulations revealed that, for both PhreeqC and HeapSim, the relation pH = −log[H_2SO_4] holds between [H_2SO_4] and pH to very good approximation, representing single proton dissociation from H_2SO_4 and a proton activity coefficient of unity.
First-order exchange and spherical diffusion models of heap leaching in PhreeqC

Using the same diffusion time (which appears in Table II) for PhreeqC and HeapSim. Therefore PhreeqC and HeapSim correlate best over longer-period leaching (150 days in this case) when a common diffusion time is used, as opposed to the short-term pulse test (2 days for the data in Figure 4) where HeapSim indicates a shorter diffusion time than PhreeqC.

It is consistently observed throughout Figures 5 to 8 that the PhreeqC model yields apparently early breakthrough of some acid to the drainage solution, which is not observed in HeapSim. This results essentially from a combination of the pH derived from the solution speciation in PhreeqC and the conversion from pH to the effective acid concentrations reported here.

Spherical diffusion model with leaching reaction and GAC

Two simulations were performed with GAC, one using the GAC rate fitted to the Tschudi production data (of 0.037 h⁻¹) and another with a relatively high rate (0.1 h⁻¹). In this case, there is more deviation evident between the two models than was the case with zero GAC, as shown in Figure 7. It is found that PhreeqC predicts a faster rate of Cu extraction and a slower rate of GAC, and the deviation becomes larger with increasing rate of GAC. The somewhat different responses of the [H₂SO₄] curves in Figure 7 are attributed to the different strategies according to which acid is allocated to the competing copper dissolution and GAC reactions. In HeapSim,
acid is attributed during each time step to each reaction in proportion to the prevailing reaction rate. In contrast, PhreeqC performs a number of smaller incremental reaction steps within each time step with all reactions in turn, up to the end of the time step or until either reagent or reactant is exhausted.

The calculation of the rate of GAC according to Equation [11] results in the GAC curve in Figure 7e becoming linear once the drainage acid concentration of Figure 7a approaches a pseudo-steady value around day 50.

**Spherical diffusion model with leaching reaction, GAC and curing acid**

When curing acid equivalent to around 45% of the total acid demand over the leaching period is added prior to leaching, the trends coincide much better, as per Figure 8. This is attributed to the more abundant availability of acid to satisfy the requirement of both the CuO and gangue reactions, with less competition for acid.

Due to the differences discussed above in the manner that PhreeqC and HeapSim assign acid to the gangue- and copper-dissolution reactions respectively, the drainage acid concentration is predicted by PhreeqC to drop rapidly from an initial high level, while HeapSim predicts that the acid concentration rises rapidly from a lower value.

**Resource optimization**

PhreeqC is considerably slower than HeapSim to execute, requiring of the order of ten times the processing time for the spherical diffusion model (e.g., 2.5 minutes vs. 15 seconds to simulate 150 days at a 10-hour time step on a notebook computer with a 2.5 GHz i5 processor). This is primarily attributed to the fact that PhreeqC solves solution speciation calculations during each iteration of each cell, while HeapSim (being custom-coded for a single known application) more pragmatically employs molecular species without considering all possible dissociation reactions.

It is therefore important to optimize the selection of the spatial grid and time interval to achieve rigorous results within the minimum possible processing time. For HeapSim, a minimum of four radial increments, a minimum of eight vertical increments, and a time step of no more than 24 hours are recommended (the time step might need to be shorter if very fast kinetics are involved); these were adopted as a first indication for the minimum standard for PhreeqC.

The PhreeqC processing time is directly proportional to the number of cells and decreases logarithmically with increasing time step size. Due to the relationship between the time step and mixing factor as per Equation [8], changing the time step (or the vertical increment that will affect the time step as per Equation [7]) will affect the MIX factors.

The optimum combination for PhreeqC has been found to be ten vertical increments and a time step of 10 hours, which yields results very similar to those produced by combinations of finer grids and smaller time steps, while still satisfying the stability criterion for MIX factors discussed above.

**Discussion and conclusions**

The first-order exchange PhreeqC model reproduced published pulse-test data extending over about two days fairly convincingly; however, its correlation to longer-term leach curves was considerably poorer.

The diffusional PhreeqC model emulated more successfully the HeapSim leach curves that had been fitted to 150 days of data from the Tschudi oxide copper heap leach operation in Namibia. This applied to conditions including and excluding gangue acid consumption and curing acid addition. The differences in results obtained in the case where the leaching and GAC reactions were competing for a
First-order exchange and spherical diffusion models of heap leaching in Phreeqc

limited acid supply is attributed to differences in the manner in which the two models assign reagent (H₂SO₄ in this case) to competing reactions. Differences in the methods of advancing the liquid phase between time steps also contributed to some of the observed differences in calculation results.

The HeapSim results fitted the published data of a short-term pulse test (over 2 days) best with a relatively short diffusion time, compared to the optimally fitted diffusion time required for the Phreeqc models. However the longer-term leach curves (150 days in this case, which of course represents the more practically meaningful application) were optimally fitted by using equal diffusion times for HeapSim and Phreeqc.

One of the disadvantages of Phreeqc compared to HeapSim is that it is slower to execute. Furthermore, heats of reaction and dilution are not accounted for. Therefore Phreeqc does not perform adiabatic calculations, although it allows manual specification of the temperature and pressure of streams and calculates the new temperatures of mixtures of streams with different temperatures. For many percolation-type problems this does not represent a limitation; in fact, it offers the convenience that heats of formation are not required as inputs. However, for oxidative high-temperature heap bioleaching applications, accounting adiabatically for the strongly exothermic reactions and for the heat and mass transfer between the solution and gaseous phases flowing countercurrently would require extensive additional coding by the Phreeqc user.

Advantages of Phreeqc are that it relies on readily available freeware that includes a considerable database of equilibrium and dissociation constants. It further offers a large number of working examples that are likely to include a good starting point for most applications suited to Phreeqc. Little user coding is required and the model structure can very easily be adapted to different uses, making it ideal for engineers and researchers who are not expert coders of modelling software. For example, the different physical and reaction kinetic properties for different parts of a heap can be specified relatively simply, which would facilitate model studies that cannot be done in HeapSim without laborious re-coding. One such example is the modelling of the effects of segregation and stratification on heap leaching performance, phenomena which are widely acknowledged as being of importance in heap leaching, but that have not been studied much to date.

Acknowledgements

This paper is published with the permission of Mintek and the University of Cape Town. The operational data made available by Weatherly’s Tschudi heap leach operation in Namibia for the 2016 conference paper is gratefully acknowledged. D.L. Parkhurst of the online Phreeqc Users’ Forum was helpful in clarifying aspects of mass balancing around Phreeqc TRANSPORT blocks, although the corresponding author accepts all responsibility for the manner in which it has been interpreted and applied in this text.
First-order exchange and spherical diffusion models of heap leaching in Phreeqc

The modelling results in this paper were first presented in 2016 at the Hydrometallurgy Conference of the SAIMM: Sustainable Hydrometallurgical Extraction of Metals (van Staden and Petersen, 2016). They are presented here with expanded background information as well as improved formulations of the governing mass transfer equations in terms of the diffusion time as the mass transfer characteristic, instead of the combination of diffusivity and diffusional path length. The graphs in Figures 6 to 8 were replotted on the basis of a common diffusion time for Phreeqc and Phreeqc, and data for a second gangue acid consumption rate was added to Figure 8.

J. Petersen wishes to acknowledge the South African National Research Foundation (NRF) for support under its Incentive Funding for Rated Researchers programme under fund number IPRR UID 85864. The opinions, findings, and conclusions expressed in this paper are those of the authors and the NRF accepts no liability whatsoever in this regard.

References


Nomenclature

Abbreviations

CAA Curing acid addition
COM Component Object Module
Cor. Pearson’s correlation coefficient
GAC Gangue acid consumption
TAC Total acid consumption
VBA Visual Basic for Applications

Roman symbols

\( A_{ij} \) Contact area between cells \( i \) and \( j \) in the Phreeqc diffusional model

\( C_{ij} \) Concentration of soluble species \( j \) in phase \( i \) [g/mol/L]; [g/mol/kg_solution]

\( C_i^0 \) Initial \( (t = 0) \) concentration of soluble species \( i \) in phase \( j \) [g/mol/L]; [g/mol/kg_solution]

\( Cor. \) Pearson’s correlation coefficient

\( D \) Free diffusivity of dissolved species in solution [m^2/s]

\( D_{ec} \) Effective diffusivity through the ore matrix [m^2/s]

\( f_{bc} \) Correction factor for the mobile/immobile boundary in the equation for \( M_{ij} \).
First-order exchange and spherical diffusion models of heap leaching in PhreeqC

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$s_{x,1}$</td>
<td>Shape factor of van Genuchten relating the first-order exchange coefficient to an equivalent diffusional path length [dimensionless]</td>
</tr>
<tr>
<td>$h$</td>
<td>Distance between mid-points of neighbouring cells in the diffusional PhreeqC model [m]</td>
</tr>
<tr>
<td>$K_i$</td>
<td>Reaction rate constant of species $i$ [h$^{-1}$]</td>
</tr>
<tr>
<td>$M_i$</td>
<td>Molar mass of species $i$</td>
</tr>
<tr>
<td>$MX_{ij}$</td>
<td>PhreeqC MIX factor from a neighbouring immobile cell number $i$ into the central cell number $j$.</td>
</tr>
<tr>
<td>$N_\text{s}$</td>
<td>Number of radial (lateral) increments, number of immobile cells associated with each mobile cell</td>
</tr>
<tr>
<td>$N_\text{z}$</td>
<td>Number of vertical increments</td>
</tr>
<tr>
<td>$P_n$</td>
<td>Geometric correction factor for immobile cell number $n$ away from the mobile zone</td>
</tr>
<tr>
<td>$r$</td>
<td>Position along the radius of the immobile zone [m]</td>
</tr>
<tr>
<td>$R$</td>
<td>Radial dimension of the immobile zone [m]</td>
</tr>
<tr>
<td>$t$</td>
<td>Time [h]</td>
</tr>
<tr>
<td>$\Delta t$</td>
<td>Time increment [s], [h], or [d]</td>
</tr>
<tr>
<td>$u$</td>
<td>Superficial solution flow velocity [m$^3$/m$^2$.h$]$</td>
</tr>
<tr>
<td>$v$</td>
<td>Velocity of solution flow through the mobile zone [m/h]</td>
</tr>
<tr>
<td>$V_j$</td>
<td>Volume of cell $j$ in the diffusional PhreeqC model [m$^3$]</td>
</tr>
<tr>
<td>$W_i^0$</td>
<td>Mass fraction of species $i$ in the solid phase at $t = 0$</td>
</tr>
<tr>
<td>$X_i$</td>
<td>Extent of conversion of species $i$ [dimensionless]</td>
</tr>
<tr>
<td>$X_i^t$</td>
<td>Rate of conversion of species $i$, [h$^{-1}$]</td>
</tr>
<tr>
<td>$\Delta z$</td>
<td>Vertical increment [m]</td>
</tr>
</tbody>
</table>

**Greek symbols**

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\alpha$</td>
<td>Exchange coefficient between mobile and immobile cells of the first-order exchange models [s$^{-1}$]</td>
</tr>
<tr>
<td>$\Gamma$</td>
<td>Diffusion time [s]</td>
</tr>
<tr>
<td>$\theta_{\text{mob}}, \theta_{\text{imm}}$</td>
<td>The volumetric fraction of the ore bed occupied by respectively mobile and immobile solution [dimensionless]</td>
</tr>
<tr>
<td>$\bar{z}$</td>
<td>Dimensionless distance along the radius of the immobile zone</td>
</tr>
<tr>
<td>$\rho$</td>
<td>Bulk density [kg/m$^3$]</td>
</tr>
<tr>
<td>$\phi$</td>
<td>Power of the unreacted fraction [dimensionless]</td>
</tr>
</tbody>
</table>

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Investigation of secondary zinc oxides as an alternative feed to the Skorpion Zinc process: Part 1 — leaching alternative zinc oxides

by C. Lottering*† and C. Dorfling†

Synopsis

Skorpion Zinc processes zinc oxide ore using a sulphuric acid leaching, solvent extraction, and electrowinning process to produce Special High Grade zinc. The company investigated the possibility of supplementing ore with alternative zinc oxide sources to extend the life of mine and maximize zinc production. In part 1 of this two-part communication we report on experimental investigations to assess the technical feasibility of recovering zinc from electric arc furnace (EAF) dust, zinc dross, and zinc fume under the current Skorpion Zinc leaching conditions. The metal dissolution and acid consumption were determined at temperatures between 40 and 70°C and pH values between 1.2 and 2.1 for slurries containing 20% solids.

With the current Skorpion Zinc operating conditions of 50°C and pH 1.8, zinc dissolution from the EAF dust, zinc dross, and zinc fume was 95, 96.9, and 98.5 %, respectively. The rate of zinc leaching from dross and from zinc fume decreased as the pH was increased to 1.5 and 1.8, respectively. The rate-determining step for zinc leaching from zinc fume gradually changed with an increase in pH from porous layer mass transport to chemical reaction and/or boundary layer mass transport. In the case of EAF dust, increasing the temperature to 70°C significantly reduced the zinc leaching rate due to the precipitation of calcium sulphate, which inhibited the zinc leaching reactions. The overall acid consumptions for all three alternative oxides investigated were below the current target consumption of 1.5 t acid per ton of Zn in the feed. It would be technically feasible to use EAF dust, zinc dross, and/or zinc fume as supplementary feed to the Skorpion Zinc process.

Keywords

hydrometallurgy, leaching, secondary zinc oxides.

Introduction

Background and objectives

Skorpion Zinc is located in the southwestern Sperrgebiet region in Namibia; it is one of the few mines in the world with an economically viable oxide zinc deposit. The ore is processed in a sulphuric acid leaching circuit under atmospheric conditions, followed by solvent extraction and electrowinning. Zinc casting is finally used to produce 99.995% Special High Grade zinc as final product.

As the orebody is becoming depleted, it becomes increasingly difficult to maintain the targeted production. Skorpion Zinc has investigated several options to ensure that the targeted production levels can be maintained until mine closure despite the declining ore resource. One option involves supplementing the remaining Skorpion Zinc ore with secondary zinc oxides such as electric arc furnace (EAF) dust, galvanizer dross, and natural-state zinc oxide to maintain the plant feed grade and production rate.

The existing plant has been optimized for processing of Skorpion Zinc ore, which differs from alternative zinc oxides in terms of composition and elemental matrices. This study therefore aimed to evaluate the feasibility of supplementing the feed to the Skorpion Zinc plant with alternative zinc oxides. The main objectives were threefold: firstly, to assess whether it is technically feasible to process alternative zinc oxides at the current Skorpion Zinc plant operating conditions; secondly, to determine the maximum amount of different alternative zinc oxides that could be fed to the plant considering operational limitations; and thirdly, to perform an economic analysis to identify the most profitable manner in which to manage supplementation of ore with alternative zinc oxide sources.

Part 1 of this communication deals with the first objective. Here, we report the results of laboratory leaching tests to investigate the effect of key operating conditions on the leaching of zinc and impurities from EAF dust, zinc dross, and natural-state zinc oxide. These test results were used subsequently to address the second and third objectives, which are discussed in Part 2 of this communication.

Skorpion Zinc leaching conditions

The Skorpion Zinc process consists of three major steps, namely atmospheric leaching, solvent extraction, and electrowinning. Of primary interest for the first part of the study is the atmospheric leaching stage, which treats ore that has been crushed and milled to 80%
Investigation of secondary zinc oxides as an alternative feed to the Skorpion Zinc process: Part 1

Due to the lower purity of the dross, it cannot be recycled directly to the furnaces for recovery, and it is therefore often treated by alternative processes.

Galvanizer dross typically consists of metallic zinc, zinc oxide, zinc chloride, some copper, iron, lead, aluminium, and other impurity elements in both oxide and metallic forms (Behnajady, Babaeidehkordi and Moghaddam, 2014; Dvořák and Jandová, 2005; Shitov et al., 2005). The majority of the metallic zinc typically reports to the coarser size fractions, while the fine size fractions normally contain the oxidized metals and a larger fraction of inclusions (Rabah and El-Sayed, 1995).

Zinc oxide from smelting furnace fumes

Zinc oxide can be collected as a waste product from lead and zinc smelters. These fuming-furnace zinc oxide powders typically contain large amounts of zinc and lead, along with some other metals such as aluminium and cadmium. As previously mentioned, ZnO is easy to leach at moderate temperature and pH conditions. Contaminated sources of zinc oxide generally contain a spinel in addition to the zinc oxides, in which some of the zinc particles are bound. This spinel, which can be represented as Mn1-x(Zn, Mg, Ni)x(Al, Cr)2O4, is insoluble and results in incomplete zinc dissolution (Jandová et al., 1999). These zinc oxides also normally contain large quantities of chlorides and fluorides, which can cause downstream processing issues (Li et al., 2015).

Leaching of alternative zinc oxides

Various studies have investigated the effects of operating variables such as temperature, particle size distribution, acid concentration, agitation rate, pH, solids content, residence time, and mineralogy on the leaching of metals from secondary zinc oxides (Cruells, Roca, and Nunez, 1992; Dvořák and Jandová, 2005; Elgersma et al., 2006, 2005; Herrero et al., 2010; Hoang Trung, Havlík, and Miškufová, 2015; Jandová et al., 1999; Jha, Kumar, and Singh, 2001; Langová et al., 2007; Moradi and Monhemius, 2010; Oustadakis et al., 2010; Pećina et al., 2008; Rabah and El-Sayed, 1995; Rusen, Sunkar, and Topkaya, 2008; Shawabkeh, 2010). In general, the effects of the different process variables depended on the composition of the secondary zinc oxides as well as the range of conditions investigated. In this study, secondary zinc oxides were obtained from suppliers that would be considered favourably for supplying material in bulk to the Skorpion Zinc processing plant. The processing conditions for the alternative zinc oxides were furthermore governed by the typical operating conditions currently employed by Skorpion Zinc for ore leaching.

Zn oxides such as zincite tend to dissolve with relative ease according to Equation [4] (Havlík et al., 2004). Zinc ferrite, which is more difficult to dissolve, reacts with sulphuric acid to form zinc sulphate and ferric sulphate, according to Equation [5] (Turan, Altundoğan, and Tümen, 2004), Equation [6] (Havlík et al., 2004), or Equation [7] (Havlík et al., 2006). According to Havlík et al. (2004) and Hoang Trung, Havlík, and Miškufová (2015), Equation [5] takes place preferentially over Equations [6] and [7] for the leaching of zinc ferrite in the temperature range from 25°C to 100°C. This reaction has slow kinetics at low temperature and
Investigation of secondary zinc oxides as an alternative feed to the Skorpion Zinc process: Part 1

the reaction rate increases significantly with temperature (Havlik et al., 2004).

\[
\begin{align*}
\text{ZnO} + \text{H}_2\text{SO}_4 & \rightarrow \text{ZnSO}_4 + \text{H}_2\text{O} \\
\text{ZnFe}_2\text{O}_4 + 4\text{H}_2\text{SO}_4 & \rightarrow \\
\text{ZnSO}_4 + \text{Fe}_2(\text{SO}_4)_3 + 4\text{H}_2\text{O} \\
\text{ZnFe}_2\text{O}_4 + \text{H}_2\text{SO}_4 & \rightarrow \\
\text{ZnSO}_4 + \text{Fe}_2\text{O}_3 + \text{H}_2\text{O} \\
\text{ZnFe}_2\text{O}_4 + 4\text{H}_2\text{SO}_4 + \text{H}_2\text{O} & \rightarrow \\
\text{ZnSO}_4 + 2\text{Fe(OH)}_3
\end{align*}
\]  

Equation [9] (Cruells et al., 2015).

While Montenegro et al. (2013) reported that dissolution of iron species is slow at sulphuric acid concentrations below 1 M, magnetite and haematite are leached readily with sulphuric acid under the experimental conditions considered for zinc ferrite leaching. The reaction of haematite with sulphuric acid is shown in Equation [8] (Turan et al., 2004); magnetite leaching with sulphuric acid proceeds according to Equation [9] (Cruells et al., 1992), or Equation [10] in the presence of atmospheric oxygen (Kukurugya, Vindt, and Havlik, 2015).

\[
\begin{align*}
\text{Fe}_2\text{O}_3 + 3\text{H}_2\text{SO}_4 & \rightarrow \text{Fe}_2(\text{SO}_4)_3 + 3\text{H}_2\text{O} \\
\text{Fe}_2\text{O}_3 + 4\text{H}_2\text{SO}_4 & \rightarrow \\
\text{FeSO}_4 + \text{Fe}_2(\text{SO}_4)_3 + 4\text{H}_2\text{O} \\
4\text{Fe}_2\text{O}_3 + 18\text{H}_2\text{SO}_4 + \text{O}_2 & \rightarrow \\
6\text{Fe}_2(\text{SO}_4)_3 + 18\text{H}_2\text{O}
\end{align*}
\]  

Equipment and procedure

All leaching experiments were conducted using the standard Skorpion Zinc leach test procedure to ensure that the results were comparable with plant tests performed previously. The experiments were performed in a 5 L tall form glass beaker of even diameter equipped with a variable-speed overhead agitator and 50 mm four-blade impeller for mixing. 500 mL demineralized water was placed in the leaching vessel, and the vessel placed in a temperature-controlled water bath. The overhead stirrer was switched on at the desired agitation rate, and the solids were then added to the water and the slurry was allowed to heat to the set-point temperature. Synthetic leach solution resembling the plant raffinate solution and containing 70 g/L sulphuric acid was then added to the slurry to achieve the desired pH for leaching. The pH of the leach solution was measured using a handheld Mettler Toledo pH meter and controlled manually through the addition of 500 g/L sulphuric acid from a burette above the leaching vessel.

Slurry samples were taken every 10 minutes during the first 75 minutes of leaching, and every 20 minutes thereafter for a total leaching time of 135 minutes. These samples were filtered using 0.45 μm syringe filters and the liquid submitted for full elemental analysis by inductively coupled plasma atomic emission spectroscopy (ICP-AES) as well as free acid analysis by titration. Upon completion of each experiment, of the alternative zinc oxides tested in this project is presented in Table 1. All materials were dried in an oven at 50°C until no further change in mass was recorded, and then sieved and pulverized for the leaching tests.

The different alternative zinc oxides had different particle size distributions. The material as received from each supplier was classified as the coarse size fraction. A normal size fraction was prepared for each of the materials by sieving a portion of the material to ~180 μm, which is the typical particle size requirement for the current Skorpion Zinc operation. The oversized material was then pulverized for 10 seconds and the process repeated until 80% of the material had a top size of 180 μm. A third size class, the fine fraction, was obtained by pulverizing a portion of the as-received material until all material had a 180 μm top size in the case of EAF dust and zinc fume. For zinc dross, aggregation of zinc particles prevented reduction of all particles to smaller than 180 μm and the milling time was selected based on the EAF dust and zinc fume milling times instead. Particle size distributions were determined by a combination of wet screening and laser diffraction utilizing a Malvern particle size analyser.

Experimental

Materials

Factors that were considered when selecting potential suppliers of alternative zinc oxides included their location, cost of the material, pre-processing requirements, and zinc concentration and impurity content of the material. The three different materials that were selected as potentially economically feasible supplementary feed to the Skorpion Zinc process included an EAF dust, zinc dross from a hot-dip galvanizer, and zinc oxide produced from the furnace fumes of a zinc smelter. A summary of the elemental compositions of the alternative zinc oxides tested in this project is presented in Table 1. All materials were dried in an oven at 50°C until no further change in mass was recorded, and then sieved and pulverized for the leaching tests.

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Synthetic leach solution was prepared for all leaching tests using zinc sulphate heptahydrate, analytical grade sulphuric acid, and demineralized water.

<table>
<thead>
<tr>
<th>Materials</th>
<th>Zn</th>
<th>Fe</th>
<th>Al</th>
<th>Ca</th>
<th>Cu</th>
<th>Ni</th>
<th>Si</th>
<th>Mn</th>
<th>Mg</th>
</tr>
</thead>
<tbody>
<tr>
<td>EAF dust</td>
<td>30</td>
<td>13</td>
<td>1.5</td>
<td>2.5</td>
<td>0.20</td>
<td>0.02</td>
<td>1.8</td>
<td>1.0</td>
<td>1.3</td>
</tr>
<tr>
<td>Zn dross</td>
<td>60</td>
<td>2.5</td>
<td>6.6</td>
<td>1.0</td>
<td>0.10</td>
<td>0.15</td>
<td>0.90</td>
<td>0.10</td>
<td>0.17</td>
</tr>
<tr>
<td>Zn fume</td>
<td>78</td>
<td>0.70</td>
<td>0.10</td>
<td>0.15</td>
<td>0.15</td>
<td>0.00</td>
<td>1.7</td>
<td>0.06</td>
<td>0.01</td>
</tr>
</tbody>
</table>

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Investigation of secondary zinc oxides as an alternative feed to the Skorpion Zinc process: Part 1

The effects of agitation speed and particle size distribution (PSD) on the leaching behaviour were evaluated independently in tests performed using a slurry containing 20% solids (normal size class) at 50°C and pH 1.8. The agitation speed was varied between 500 and 700 r/min to determine the agitation speed required to eliminate the effect of boundary layer mass transfer on the zinc dissolution kinetics. For each of the alternative zinc oxide materials, three samples with qualitatively different particle size distributions (i.e., fine, normal, and coarse) were prepared as described above to investigate the effect of the PSD on the leaching behaviour. Subsequently, the effects of leaching temperature and pH on the leaching behaviour of the different alternative zinc sources were evaluated according to a full factorial experimental design. Temperatures of 40, 50, 60, and 70°C and pH values of 1.2, 1.6, 1.8, and 2.1 were investigated for a slurry containing 20% solids in the normal size class. Other operating conditions such as residence time were selected based on the current Skorpion Zinc processing conditions and plant limitations.

Results and discussion

Effect of agitation speed

The effect of agitation on zinc leaching rate was investigated to determine the agitation speed required to prevent mass transport in the boundary layer from limiting the zinc leaching reactions in the experimental set-up. Increasing the agitation rate from 500 r/min to 700 r/min had an insignificant effect on the rate of zinc dissolution and the extent of zinc leaching achieved after 135 minutes, as shown in Figure 1. An agitation rate of 700 r/min was used in all subsequent tests. The differences in rate and extent of zinc leaching observed for the coarse fraction may be

Effect of PSD

The PSDs for the different size classes in Table II indicate differences in the breakage behaviour of the different zinc oxide materials. In the case of zinc dross, the fine sample (prepared by pulverizing all material) had a wider size distribution than the normal size distribution (prepared by pulverizing the particles larger than 180 μm). The zinc dross contained a large fraction of metallic zinc. The perceived increase in the particle size with increased pulverization might have been due to metallic particles in the dross being flattened rather than shredded or ground during pulverizing. The finer dust component of the dross furthermore contained small metallic particles which could have undergone aggregation during pulverization.

As expected, the particle sizes decreased during leaching. Reduction in the size of particles in the larger size fractions

Table II

<table>
<thead>
<tr>
<th>Material</th>
<th>Size class</th>
<th>$P_{10}$ (μm)</th>
<th>$P_{50}$ (μm)</th>
<th>$P_{90}$ (μm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>EAF dust</td>
<td>Coarse</td>
<td>1</td>
<td>24</td>
<td>379</td>
</tr>
<tr>
<td></td>
<td>Normal</td>
<td>2</td>
<td>41</td>
<td>264</td>
</tr>
<tr>
<td></td>
<td>Fine</td>
<td>1</td>
<td>3</td>
<td>87</td>
</tr>
<tr>
<td>Zn dross</td>
<td>Coarse</td>
<td>11</td>
<td>209</td>
<td>586</td>
</tr>
<tr>
<td></td>
<td>Normal</td>
<td>4</td>
<td>72</td>
<td>172</td>
</tr>
<tr>
<td></td>
<td>Fine</td>
<td>5</td>
<td>151</td>
<td>391</td>
</tr>
<tr>
<td>Zn fume</td>
<td>Coarse</td>
<td>9</td>
<td>603</td>
<td>1074</td>
</tr>
<tr>
<td></td>
<td>Normal</td>
<td>1</td>
<td>22</td>
<td>193</td>
</tr>
<tr>
<td></td>
<td>Fine</td>
<td>1</td>
<td>8</td>
<td>232</td>
</tr>
<tr>
<td>EAF dust</td>
<td>Coarse</td>
<td>2</td>
<td>44</td>
<td>239</td>
</tr>
<tr>
<td></td>
<td>Normal</td>
<td>1</td>
<td>7</td>
<td>101</td>
</tr>
<tr>
<td></td>
<td>Fine</td>
<td>1</td>
<td>4</td>
<td>28</td>
</tr>
<tr>
<td>Zn dross</td>
<td>Coarse</td>
<td>17</td>
<td>191</td>
<td>446</td>
</tr>
<tr>
<td></td>
<td>Normal</td>
<td>1</td>
<td>3</td>
<td>7</td>
</tr>
<tr>
<td></td>
<td>Fine</td>
<td>2</td>
<td>39</td>
<td>190</td>
</tr>
<tr>
<td>Zn fume</td>
<td>Coarse</td>
<td>1</td>
<td>105</td>
<td>393</td>
</tr>
<tr>
<td></td>
<td>Normal</td>
<td>1</td>
<td>3</td>
<td>13</td>
</tr>
<tr>
<td></td>
<td>Fine</td>
<td>1</td>
<td>3</td>
<td>8</td>
</tr>
</tbody>
</table>

in some instances resulted in increased 10% and 50% passing sizes. The most significant size reduction was observed for the normal size class zinc dross and the fine and normal size classes of zinc fume, resulting in $P_{90}$ particle sizes of 13 μm or less.

The dissolution of zinc in the different size classes for the respective alternative zinc oxides is shown in Figure 2. While the rate and extent of zinc leaching from the fine and the normal size fractions of zinc fume were comparable, the rate and extent were significantly higher than for the coarse size fraction. The faster leaching rate observed for the smaller particles can be ascribed to the larger specific surface area available for the leaching reactions to proceed. The lower extent of leaching observed for the coarse fraction may be
due to the entrapment of ZnO in insoluble impurity substances such as spinel phases. After leaching, the $P_{90}$ particle size of 393 μm for the coarse fraction was 50 to 50 times larger than the $P_{90}$ particle sizes for the other size classes.

Considering that the normal size class of the zinc dross had a smaller particle size distribution than the fine class, the results shown in Figure 2c agree with results presented by Moradi and Monhemius (2011); as was the case for the zinc fume samples, decreasing the particle size resulted in faster zinc leaching. The greater difference in the zinc dissolution between the fine size class and the normal size class for zinc dross, compared to zinc fume, can be ascribed to the larger difference in the particle size distributions of the different size classes for the respective materials. It is also noticeable that the zinc leaching rates from the fine and the normal size classes of zinc dross are slower than from same size classes of zinc fume, presumably due to the larger particle size distributions as well as the aggregation of fine metallic particles during pulverization, which decreased the specific surface area of the material and hence the expected leaching rate.

In the case of EAF dust, leaching of zinc from the coarse size fraction proceeded at a faster rate than in the case of zinc fume and zinc dross because of the smaller particles present in the EAF dust coarse fraction. This also suggested that the large particles leached readily under the conditions investigated. Although the initial leaching rate was slightly slower than for the fine and normal size classes, 89% zinc dissolution was achieved after 25 minutes. Zinc dissolution from the normal and coarse size fractions reached equilibrium after 25 minutes, after which there was no significant change in the eluate zinc concentration. For fine particles, zinc dissolution increased gradually from 80% after 25 minutes to 95% after 135 minutes, resulting in a slightly higher overall zinc dissolution compared to the normal and coarse size classes.

Although there was an increase in iron dissolution over this time period (refer to Figure 3), the amount of iron dissolved was stoichiometrically less than what would be expected if the zinc leaching during this period was due solely to zinc ferrite dissolution. This suggested that a portion of the zinc species was either entrapped in insoluble phases or coated with an inert product layer which formed during the initial leaching period. Dissolution of these zinc species was possible only in the fine size class, where the diffusion path through the insoluble phases was sufficiently short for leaching of the zinc species to occur within the time period investigated. The slow zinc leaching rate from 25 minutes to 135 minutes confirmed that a different mechanism (e.g. diffusion through insoluble layers) controlled the leaching rate. Had the effect been due purely to improved liberation, the liberated zinc would have likely dissolved at a rate comparable to the rate observed in the first 25 minutes.

Trends in aluminium dissolution generally followed those for zinc leaching. 45–66% aluminium leaching was achieved for zinc dross, and 30–50% and 35–55% for EAF dust and zinc fume, respectively. The highest iron dissolution of 84% was observed for the normal size class zinc dross, as shown in Figure 3. Iron dissolution was the lowest for EAF dust, ranging between 17 and 21%. Given the relatively high zinc recoveries, the undissolved iron could not be in the form of zinc ferrite or zinc-containing spinel phases; iron was therefore likely to be present mainly as stable oxide phases in the EAF dust, which could inhibit dissolution of entrapped zinc species. Leaching of the zinc fume sample resulted in 35–58% iron dissolution. As was the case for the other materials, the iron concentration increased gradually between 25 minutes and 135 minutes without reaching equilibrium in this time period. The distribution of iron between zinc ferrite and the slow-leaching iron oxide species determined the overall iron dissolution.

**Effect of temperature and pH**

**Zinc leaching**

The effect of temperature on the extent of zinc dissolution from zinc fume after 155 minutes was negligible at pH 1.2,
Investigation of secondary zinc oxides as an alternative feed to the Skorpion Zinc process: Part 1

but increased with an increase in pH. Figure 4 shows that the zinc leaching extent at pH 1.2 varied between 98.4 and 99.6% at 40°C and 70°C, respectively. At pH 2.1, the corresponding zinc leaching percentages were 94.1 and 99.6%, respectively. The effect of pH on the extent of zinc dissolution after 135 minutes was again significant only at the lower temperatures, and was statistically less significant than the effect of the temperature.

For tests conducted at 40°C and 50°C, the initial rate of zinc leaching generally decreased with an increase in pH, particularly at pH values of 1.8 or higher. Although the leaching rates at 60°C for pH 1.5 and pH 1.8 are slow compared to the leaching rates observed at the other conditions, no reasons can be assigned for this behaviour. Overall, it did not appear as if the temperature had a significant effect on the rate of leaching, which suggested that these reactions were mass-transfer limited.

The effect of temperature on zinc dissolution from dross at different pH values is shown in Figure 5. In general, the rate of zinc leaching from dross decreased with increasing pH due to the lower hydrogen ion concentration. At pH 1.5 and pH 1.8, the zinc leaching rate increased with increasing temperature. At pH 1.2 and pH 2.1, however, no clear trend in the leaching rate with temperature was observed. This suggests that the rate-determining mechanism changed with a change in acid concentration, with chemical reaction rate-limiting conditions dominating in the intermediate pH range.

This hypothesis was investigated further by fitting three common leaching mechanism models to the data from the zinc dross leaching experiments conducted at 60°C. The leaching rates during the first 45 minutes are presented in Figure 6. Q represents the leaching models as reported by Prosser (1996) and shown on the left-hand side of Equations [11] to [13]; Equation [11] is the model if mass transport in the boundary layer is the rate-determining step, while Equations [12] and [13] represent the surface chemical reaction-limiting and the mass transport in porous product layer-limiting models, respectively. X refers to the fractional dissolution at time t, and k is the corresponding rate constant.

\[
1 - (1 - X)^{2/3} = kt \quad [11]
\]

\[
1 - (1 - X)^{1/3} = kt \quad [12]
\]

\[
1 - \frac{2}{3} X = (1 - X)^{2/3} = kt \quad [13]
\]

The coefficients of determination (R^2) for the different models in the respective cases are shown in Table III. At pH 1.2, the porous layer mass transport model clearly fitted the data the best; with an increase in pH, a gradual shift from porous layer mass transport-limited leaching to combination of chemical reaction- and boundary layer diffusion-limited leaching occurred.
A comparison of the overall zinc recovery at the expected plant residence time of 135 minutes for the different temperatures and pH conditions suggested that temperature had no statistically significant effect at pH 1.2. At higher pH values, the percentage zinc dissolution generally showed a slight increase with increasing temperature. Similarly, pH had a negligible effect on the extent of zinc leaching at 60°C and 70°C. At lower temperatures, the zinc dissolution decreased by up to 11 percentage points with an increase in pH to values of 1.5 or higher.

From Figure 7 it is evident that temperature and pH had no significant effect on zinc recovery from EAF dust after 135 minutes. These results suggest that the EAF dust identified as a potential alternative zinc oxide source for the Skorpion Zinc process contained a large proportion of readily leachable zinc oxide, with limited amounts of zinc ferrite. While the pH did not affect the leaching kinetics noticeably, the rate of zinc dissolution decreased when the temperature was increased above 60°C at all pH conditions investigated. This effect was observed only for leaching from EAF dust.

The elemental composition of the EAF dust and that of the zinc dross and the zinc fume differ in several respects. For example, the zinc content in EAF dust is low while the iron and impurity (Ca, Mn, and Mg) concentrations are high compared to the other materials. Given the comparative leaching rates at pH values between 1.2 and 2.1, and that the pH was controlled to the set-point value, it is unlikely that increased acid consumption by impurities at 70°C reduced the zinc leaching rate. It was, however, notable how the calcium leaching and precipitation behaviour varied for different sources and changed with temperature. No calcium precipitation was observed in the tests with fume dust, as the maximum achievable calcium concentration was below the calcium sulphate solubility limit. In the case of zinc dross, calcium precipitation did occur, but to a lesser extent than what was observed for EAF dust with the high calcium content. The calcium dissolution profiles for EAF dust at the different tests conditions are shown in Figure 8.

The average percentage calcium dissolution after 135 minutes decreased from 60% at 40°C to 47% at 70°C due to the decrease in calcium sulphate solubility with increasing temperature, as reported by Azimi, Papangelakis, and Dutrizac (2007). At 40°C and 50°C, fast initial calcium dissolution (up to 80%) was recorded at 15 minutes, followed by relatively rapid precipitation in the next 10 minutes and gradual or no further precipitation subsequently. The average drop in calcium concentration from 15 minutes to 135 minutes was 14.7 percentage points at 40°C and 18.4 percentage points at 50°C. At 60°C and 70°C, rapid precipitation is not observed because faster Ca dissolution and precipitation caused Ca to precipitate within the first 15 minutes of leaching. At 60°C, the drop in calcium

### Table III

<table>
<thead>
<tr>
<th>pH</th>
<th>k (min⁻¹)</th>
<th>Intercept</th>
<th>R²</th>
<th>k (min⁻¹)</th>
<th>Intercept</th>
<th>R²</th>
<th>k (min⁻¹)</th>
<th>Intercept</th>
<th>R²</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.2</td>
<td>0.0119</td>
<td>0.113</td>
<td>0.79</td>
<td>0.0028</td>
<td>0.030</td>
<td>0.64</td>
<td>0.017</td>
<td>0.190</td>
<td>0.74</td>
</tr>
<tr>
<td>1.5</td>
<td>0.0088</td>
<td>0.043</td>
<td>0.91</td>
<td>0.0027</td>
<td>0.001</td>
<td>0.94</td>
<td>0.014</td>
<td>0.088</td>
<td>0.88</td>
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<tr>
<td>1.8</td>
<td>0.0167</td>
<td>0.003</td>
<td>0.99</td>
<td>0.0060</td>
<td>-0.014</td>
<td>0.95</td>
<td>0.024</td>
<td>0.041</td>
<td>0.98</td>
</tr>
<tr>
<td>2.1</td>
<td>0.0058</td>
<td>0.011</td>
<td>0.99</td>
<td>0.0013</td>
<td>-0.005</td>
<td>0.94</td>
<td>0.010</td>
<td>0.029</td>
<td>0.98</td>
</tr>
</tbody>
</table>

Figure 7—Effect of temperature on the rate of zinc leaching from EAF dust at (a) pH 1.2, (b) pH 1.5, (c) pH 1.8, and (d) pH 2.1

Figure 8—Effect of pH on the rate of calcium leaching from EAF dust at (a) 40°C, (b) 50°C, (c) 60°C, and (c) 70°C
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calcium precipitation was virtually complete within the first 15 minutes at 70°C. These results suggest that the rates of calcium leaching and precipitation as calcium sulphate with increasing temperature became faster than the zinc leaching rate, resulting in partial passivation of the EAF dust particle surfaces. As a result, the extent of zinc dissolution decreased in the first 15 minutes of leaching and proceeded at a slower rate subsequently as mass transfer of reagents through the inert precipitate layer was required for the zinc leaching reactions to proceed. Decreased base metal leaching rates as a result of gypsum formation have also been reported by Gharabaghi, Iraunjad, and Azadmehr (2013), who investigated nickel leaching from zinc plant residue, as well as Seidel and Zimmels (1998) and Seidel et al. (1999), who investigated aluminium leaching from coal fly ash. While increasing temperature is generally considered a requirement for faster leaching of zinc species such as zinc ferrite, it could possibly inhibit the leaching rate if the source contains calcium in sufficiently high concentrations to form significant amounts of calcium sulphate precipitate. The formation of a precipitate layer could also have contributed to the leaching behaviour observed for the fine particle size class, as discussed previously.

Aluminium and iron leaching

The effect of temperature on aluminium and iron dissolution from zinc fume did not show a clear trend. Dissolution in the pH range 1.2 to 1.5 was generally higher than in the range 1.8 to 2.1. The aluminium content of the zinc fume was an order of magnitude lower than that of the zinc dross and the EAF dust (Table I). The resulting aluminium concentration in the pregnant leach solution (PLS) was between 25 and 37 mg/L at the investigated conditions, which is below the acceptable limits for the Skorpion Zinc circuit. Similarly, the iron concentrations in the PLS between 160 and 260 ppm would typically be acceptable for the current Skorpion Zinc processing plant. The higher percentage iron leaching from zinc fume compared to EAF dust would therefore not be a problem given the relatively low iron content of the feed material.

In general, aluminium leaching from zinc dross increased with an increase in temperature, as shown in Figure 9. Aluminium dissolutions were 84.9% or higher at 60°C, and 88.9% or higher at 70°C in the pH range 1.2–1.8. The average aluminium dissolution of 88.9% at pH 1.2 was significantly higher than at the other pH conditions (between 65.4% and 67.4%). The effects of temperature and pH on aluminium leaching were similar to the effects observed for zinc leaching; this could be attributed to the presence of aluminium and zinc in similar forms in the dross. The trends observed for iron dissolution from zinc dross followed aluminium leaching closely, with high iron dissolution observed at pH 1.2 (88.6–94.0%) and at 60°C and 70°C in the pH range 1.2 to 1.8.

The 65% aluminium dissolution and 85% iron dissolution achieved at pH 1.8 and 50°C, which resemble typical current Skorpion Zinc operating conditions, might have a detrimental effect on downstream processing considering the relatively high impurity content of zinc dross. The resulting PLS had aluminium and iron concentrations in the range of 2.8–4.9 g/L and 1.4–2.4 g/L, respectively. The limitations that this places on the amount of dross that can be treated due to limited impurity removal capacity are investigated in more detail in part 2 of this communication.

The trends for aluminium dissolution from EAF dust were similar to those for zinc dross. The differences in percentage extraction were insignificant in the pH range 1.5 to 2.1, but the average aluminium dissolution of 67.3% at pH 1.2 was between 17 and 21 percentage points higher than at the other pH values. Although the greatest extent of aluminium dissolution in the pH range 1.5 to 2.1 occurred at the highest temperature, aluminium leaching appeared to be less sensitive to temperature at the lowest pH. Due to the lower percentage of aluminium leaching from, and lower aluminium content of, EAF dust compared to dross, the resulting PLS had a lower aluminium concentration, in the range 70–370 mg/L. From Figure 10 it can be seen that the extent of iron leaching from EAF dust was on average 45 and 27 percentage points lower than iron leaching from zinc dross and zinc fume, respectively. This was due mainly to presence of iron primarily in the form of iron oxides rather than zinc ferrite, which appeared to leach readily at the investigated conditions. The relatively low percentage iron dissolution and the presence of stable iron compounds also support the inhibition of zinc leaching due to entrapment of zinc species in insoluble iron phases. The iron content of the EAF dust was, however, more than five times higher than the iron content of the other materials. This resulted in relatively high iron concentrations in the PLS, ranging between 1.2 g/L and 8.3 g/L.
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It is desirable to minimize iron extraction while maximizing zinc extraction, as iron may lead to overloading of the SX circuit and, ultimately, to the production of zinc that does not meet product specifications. In addition, leaching of iron instead of zinc leads to higher acid consumptions, which is a major focal point for cost optimization in any leaching process.

**Acid consumption**

Due to the significant contribution of acid to the operating costs of the Skorpion Zinc leaching plant, it was important to establish the acid consumptions of the potential secondary zinc oxide sources. The results reported here are for the base case, in which the normal Skorpion Zinc operating parameters were used (i.e. 50°C, pH 1.8, and 20% feed solids slurry). Figure 11a shows the total acid consumption in the leaching circuit alone, excluding acid consumed during filtration on the residue belt filters during the acid wash stage. EAF dust had the lowest leaching acid consumption at a total of 700 kg/t of feed, while the zinc dross had the highest consumption at 1580 kg/t of feed material. The acid consumption of zinc fume, at 1100 kg/t feed, was relatively low compared to the zinc dross due to the fact that it has the lowest impurity content. The relatively high zinc content did, however, result in a higher acid consumption than for EAF dust.

Evaluation of the gangue acid consumption for the alternative zinc oxide sources confirmed this interpretation of the acid consumption data. The gangue acid consumption was calculated based on the initial acid concentration, acid addition, residual acid concentration, and the stoichiometric amounts of acid required to achieve the specific extents of zinc dissolution. It is evident from Figure 11b that the zinc dross has the highest gangue acid consumption at a total of 540 kg/t of feed material, which contributed to the overall high leaching acid consumption for this material. As expected, the zinc fume, which had the lowest impurity content, had the lowest gangue acid consumption at 47 kg/t of feed.

Although the EAF dust contains the most impurities, it had a lower gangue acid consumption than the zinc dross, at a total of 530 kg/t of feed, because of the stability of the iron phases in EAF dust and the relatively low iron dissolution observed, as discussed previously.

Skorpion Zinc targets a gangue acid consumption of 150 kg/t for the ore to ensure that operating costs remain economically viable. Based on this target, only the zinc fume sample fell within the budgeted gangue acid consumption. However, since the zinc content in these samples is significantly higher than in the Skorpion Zinc ore, it may be possible to refine the samples economically regardless of the high acid consumption. To determine this, the total acid consumption for each source, including the acid consumed during re-acidification of the thickener underflow and during acid washing of the residue on the belt filters, was determined.

The targeted total overall acid consumption for the Skorpion Zinc ore is 1.5 t per ton of zinc. From Figure 12, it is clear that the acid consumption for all three oxide sources was lower than this target. The zinc dross and EAF dust had overall consumptions of 1.0 t and 1.3 t per ton Zn, respectively; the zinc fume acid consumption is the lowest of the three at 0.1 t per ton Zn. This is expected, as the gangue acid consumption is significantly lower for zinc fume (290 kg/t lower than for EAF dust), while the zinc content is approximately 78% compared to 50% for EAF dust and 60% for zinc dross. The zinc dross overall acid consumption per ton zinc is lower than that for EAF dust acid consumption, despite a higher gangue acid consumption, because it contains approximately double the amount of zinc.

**Conclusions**

A reduction in the particle size generally resulted in faster leaching kinetics due to the larger specific surface area, liberation of entrapped zinc species, and/or reduction in diffusion path lengths. A comparison of the recovery trends for the different samples revealed that the zinc fume yielded the highest overall zinc recovery of between 94.1 and 99.6%. The dissolution of zinc from zinc dross and EAF dust varied between 86.2 and 97.8% and 88.9 and 95.9%, respectively. Temperature did not have a significant effect on the extent of zinc leaching from the fume samples, but increasing the pH to 1.8 or higher at 40°C and 50°C did reduce the zinc leaching rate.

In the case of zinc dross, the rate of zinc dissolution also decreased with an increase in pH; the extent of zinc dissolution decreased by up to 11 percentage points when the pH was increased to 1.5 or higher. In the intermediate pH range, the leaching rate and extent of leaching increased with an increase in temperature. It was concluded that the rate-determining step for zinc leaching from dross gradually changed from mass transfer in a porous layer at pH 1.2 to chemical reaction and/or boundary-layer mass transport at higher pH values.
The EAF dust tested contained a high proportion readily leachable zinc species. Temperature had a significant effect on the zinc leaching rate only when increased to 70°C, at which point zinc leaching proceeded at a slower rate than at lower temperatures. This was due to the faster precipitation of calcium sulphate, which formed a passivating layer on the soluble zinc species and inhibited zinc dissolution.

Zinc dust reported the largest total and gangue acid consumptions of 1380 kg/t and 540 kg/t of feed material, respectively. Although EAF dust had a higher impurity content than zinc dust, the gangue acid consumption for EAF dust was lower than for zinc dust due to the presence of iron in sparingly soluble phases instead of easily soluble iron species. Iron dissolution from EAF dust varied between 20.7 and 47.7%, while 43.0–94.0% iron dissolved from dross. Given the high zinc and low impurity content of the zinc fume, more than 95% of the 1100 kg acid required per ton of feed material was utilized for zinc dissolution.

The gangue acid consumption was relatively high compared to the current Skorpion Zinc target of 150 kg acid per ton of feed material. However, the overall acid consumption for all three potential alternative zinc oxide sources was below the current Skorpion Zinc target of 1.5 t acid per ton of zinc in the feed material, due to the high zinc content of the alternative oxides compared to the ore.

Considering the leaching performance, it can be concluded that all three alternative zinc oxide sources could technically be used to supplement the ore feed to the Skorpion Zinc process at the current operating conditions. The optimal source and blending ratios have to be determined by mass balances and economic analyses, which are addressed in part 2 of this communication.

Acknowledgements

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References

Behnajady, B., Babaeidehkordi, A., and Moghadam, J. 2014. Determination of balances and economic analyses, which are addressed in part 2 of this communication.

Investigation of secondary zinc oxides as an alternative feed to the Skorpion Zinc process: Part 1


A new open-pit mine planning optimization method using block aggregation and integer programming

by N.L. Mai*, E. Topal†, and O. Erten‡

Synopsis

Mathematical programming has been applied to optimizing open pit mine planning problems since the early 1960s. Nonetheless, it still remains challenging to obtain a life-of-mine plan with current computational hardware and software, mostly because of the scale of the input data, which is generally in the form of mining blocks. To overcome this challenge, a common practice is to aggregate blocks into larger units before formulating and solving mine planning models. However, the majority of available block aggregation techniques ignore the slope relation between blocks or are simply not capable of controlling the number of aggregates generated. In this study, a new optimization method for open pit mine planning is proposed, which consists of two stages. In the first stage, a new block aggregation algorithm is proposed, called the TopCone algorithm (TCA), where blocks are clustered into TopCones (TCs), which have two important features: (1) the cone shape and (2) the number of TCs that can be explicitly controlled. In the second stage, TCs form the basis of an integer programming model with a variety of operational constraints so that a high-quality production scheduling solution can be obtained in relatively quick computational time. The capability and novelty of the proposed method is demonstrated through the optimization of the long-term production schedule of a large-scale copper deposit. The case study shows higher NPV results compared to a commercial software package, and the entire mine planning process can be completed in less than 10 minutes.

Keywords

large-scale optimization, TopCone algorithm, production scheduling, open pit mine planning, integer programming.

Introduction

The fundamental input for open pit mine planning is a resource block model in three-dimensional space, which represents geological attributes, i.e., grades of different minerals comprising the orebody, mineralogy, density, and tonnage. This model can then be converted into an economic block model by applying economic parameters such as operational costs and commodity price. A critical objective of mine planning is to determine the mine’s production schedule, which basically is a plan indicating when and where to mine ore and waste blocks to maximize total discounted cash flows subject to various constraints, including blending grades, mining and processing capacity requirements, and slope safety. Optimizing the production schedule is a complicated task that involves processing a substantial amount of data constrained by many conditions, which is naturally the domain of mathematical programming techniques (Pendharkar, 1997; Topal and Ramazan, 2010, 2012).

The pioneering of the application of linear programming (LP) mathematical models in open pit mine production scheduling has been credited to Johnson (1968). In this type of model, linear variables represent mining proportions of blocks which lead to slope safety violations, as blocks at a lower level can be mined without the overlying blocks being completely removed. For this reason, integer programming (IP) and its variants, such as mixed integer programming (MIP), were developed, which consider integer variables to maintain the integrity of the mining blocks. For a block model including \( n \) blocks to be scheduled within \( p \) periods, each block will have \( p \) binary variables which enable that particular block to be scheduled (value of 1) in a period \( p \) or not (value of 0). In real mining projects, the total number of binary variables \( n \times p \) is usually enormous, with magnitudes into the millions. Many studies have suggested different ways to deal with the problem of solving large-scale production scheduling using IP (Bienstock and Zuckerberg, 2010; Bley et al., 2010; Boland et al., 2009; Caccetta and Hill, 2003; Gershon, 1983; Ramazan and Dimitrakopoulos, 2004). However, the problem still seems to present significant challenges and it is generally impossible to solve considering the computational intensiveness.

Given the difficulty of achieving an exact solution from large-scale IP models, researchers have resorted to heuristic and metaheuristic techniques to obtain near-optimal solutions within a reasonable computation time. Several notable heuristic
A new open-pit mine planning optimization method using block aggregation and integer programming and metaheuristic algorithms developed recently can be found in the works of Chicoisne et al. (2012), Jélvez et al. (2015), Lamghari, Dimitrakopoulos, and Ferland (2015), and Lamghari and Dimitrakopoulos (2012). A common drawback of these models, apart from that of Lamghari, Dimitrakopoulos, and Ferland (2015), is that they are not able to incorporate a typical full set of operational constraints in mining, being the lower and upper bounds of mining capacity, processing capacity, and blending grade.

Block aggregation is another approximation approach, in which blocks are clustered to form larger units to reduce the scale of the data before formulating mathematical models. Reducing block resolution, or reblocking, is by far the simplest method but this also seriously compromises the quality of scheduling results. One common trend is to combine blocks on the same level based on their similarity of distance, material, or attributes, such as in the work of Tolwinski and Underwood (1996) and Tabesh and Askari-Nasab (2011). As slope constraints between blocks are not considered in the clustering stage but must be considered in the production scheduling stage, this conflict may severely compromise the possibility of obtaining an optimal solution. The two most well-known block aggregation algorithms where slope constraints are effectively introduced in the aggregating process are fundamental tree algorithm (FTA) (Ramazan, 2007) and Blasor (Froyland and Menabde, 2009). FTA deploys a clever LP formulation to cluster blocks into fundamental trees (FTs). However, in most instances, the number of FTs is slightly less than the number of ore blocks in the ultimate pit limit where FTA is applied. This is because most FTs consist of only one ore block, the so-called ‘single tree problem’. Consequently, the number of FTs is still very large in real-life data-sets. Blasor uses a propagation procedure from the bottom up to cluster blocks into clumps with a cone shape, which considerably reduces the number of variables for downstream scheduling models. However, to our best knowledge, there is no evidence that Blasor can dictate or effectively control the number of clumps generated.

The above mentioned challenges are the motivation for our new algorithm, called the ‘TopCone algorithm’ or TCA. This algorithm is a hybrid of the ultimate pit limit technique and the clustering technique, where a near-optimal ultimate pit can be obtained during the clustering process. TCA aggregates blocks into ‘TopCones’ using LP in a manner such that the number of aggregates generated can be controlled to keep the size of the downstream IP-based scheduling model tractable. A schematic of a traditional and the new proposed mine planning framework is presented in Figure 1.

The remainder of this paper is organized as follows. The methodology of the TCA is presented, followed by a demonstration of the algorithm in a simple 2D case study. We then detail the methodology of a long-term production scheduling model using IP specially developed for TCs. Large-scale numerical experiments are discussed. Finally, conclusions are drawn.

Methodology of TopCone algorithm
TCs are groups of blocks having four properties:
(i) Can be mined without violating slope constraints
(ii) Total economic value of blocks as a TC must be positive
(iii) The TCs are subject to certain constraints
(iv) A TC cannot be split into smaller cones without violating (i), (ii), and (iii).

The constraints in property (iii) could be the minimum cone size (MCS) in terms of the number of member blocks or total ore or waste tonnage, average grade, material proportion, or any constraints that could appear in the downstream scheduling model. At the current stage of development, we set only the minimum number of blocks per cone as a condition for property (iii) to control the number of TCs generated. However, it would be promising to consider other attributes as they may have a beneficial impact on the result of solving the IP model.

Nomenclature
The following notations are defined for explaining the steps of the algorithm:

Indices and sets
\( i \in N \) Set of underlying nodes \( i \) found at current searching level

Figure 1 — Traditional three-stage mine planning framework (blue arrows) and the proposed two-stage framework with TopCone algorithm (red arrows)
A new open-pit mine planning optimization method using block aggregation and integer programming

$\mathcal{M}$ Set of overlying nodes $j$ established at current network flow
$\mathcal{M}_i$ Subset of overlying nodes $j$ that belong to cone of underlying node $i$
$\mathcal{U}_j$ Subset of underlying nodes $i$ which have their cone cover overlying node $j$
$s$ Source node
$t$ Sink node

Parameters
$CV_i$ Cone value of underlying node $i$
$C_i$ Coefficient of underlying node $i$
$V_i$ Economic value of block $i$. If $V_i > 0$, block $i$ is called a 'positive node', otherwise, a 'negative node'
$\varepsilon$ A very small decimal value, for example, 0.01

Variables
$f_{si}$ Continuous variables: Flow from source node to underlying node $i$
$f_{ij}$ Continuous variables: Flow from underlying node $i$ to overlying node $j$
$f_{jt}$ Continuous variables: Flow from overlying node $j$ to sink node $t$

Figure 2 gives a two-dimensional illustration of a network consisting of the abovementioned nodes and flows.

Steps of TCA
The algorithm starts from the top level of the block model and then progresses level by level until the bottom, aggregating blocks into TCs at each iteration. Steps are schematically illustrated in Figure 3 and discussed below.

Step 1. The level that the TCA is currently scanning is termed the ‘current searching level’ and all positive nodes at this level are termed ‘underlying nodes’. All nodes (both positive and negative) that overlie the underlying nodes based on slope constraints are termed ‘overlying nodes’. If no positive node is found at the current searching level, the algorithm goes to a lower level and repeats step 1.

Step 2. Generate a network flow consisting of source node, sink node, underlying nodes, overlying nodes, and flows, as illustrated in Figure 2.

Step 3. Calculate the cone values of underlying nodes by summing the economic values of all overlying nodes connected to that underlying node and the underlying node itself.

$$CV_i = \sum_{j \in \mathcal{O}_i} V_j + V_i \ \forall i \in \mathcal{N} \tag{1}$$

Figure 3—Sequence of steps of the TopCone algorithm
Step 4. Assign coefficients to \( N \) underlying nodes according to their cone values, starting from the highest and proceeding to the smallest with coefficients from 1 to \( N \) respectively. Specifically, the underlying node with the highest cone value has a coefficient of 1, the second highest has a coefficient of 2 and so on.

Step 5. Set up and solve the LP formulation. The aim of the LP model is to group positive and negative nodes into clusters (called TCs) so that in each cluster, positive nodes are strong enough to support negative nodes. That is, TCs always have a positive value. This is a minimization problem to minimize connections between nodes so that the size of the clusters is minimal.

**Objective function**

\[
\sum_{i \in \Omega_t} \sum_{j \in \Omega_i} C_i \cdot f_{ij} \]  

[2]

**Subject to**

\[
f_{ij} \leq V_i + \sum_{j \in \Omega_i} V_j, \quad \text{if } V_j > 0 \quad \forall i \in N
\]  

[3]

\[
f_{j\ell} = -v_j \cdot v_{j\ell} \cdot c_\alpha, \quad \text{if } V_j > 0 \quad \forall j \in M
\]  

[4]

\[
\sum_{i \in \Omega_j} f_{ij} - f_{j\ell} = 0 \quad \forall j \in M
\]  

[5]

\[
f_{ii} - \sum_{j \in \Omega_i} f_{ij} = 0 \quad \forall i \in N
\]  

[6]

\[
f_{i\ell}, f_{\ell j}, f_{j\ell} \geq 0 \quad \forall i \in N, \forall j \in M
\]  

[7]

The objective function is a minimization of connections between underlying nodes and their corresponding overlying nodes. The role of the coefficients is to prioritize the creation of connections to high-value nodes so that they are available for mining before the lower value ones. This is critical to facilitate downstream scheduling models to yield a better NPV score. Constraint [3] ensures that an underlying node and its positive overlying nodes are unable to provide support greater than their value. Constraints [4] ensure that overlying nodes receive sufficient support. Note that positive overlying nodes can always be removed (i.e. just need a very small \( \epsilon \) value support) once the corresponding underlying nodes are removed. The addition of \( \epsilon \) is to prevent TCs from having zero value as this will require additional support from other underlying nodes. Constraints [5] and [6] enforce the mass balance of flows in and out of the underlying and overlying nodes. Constraint [7] defines all flows as non-negative linear variables.

As this is a pure linear problem, it can be solved quickly using a solver like CPLEX (CPLEX, 2009).

Step 6. Analyse the LP solution by checking all positive flows to find TCs. Once generated, TCs automatically respect and accommodate slope constraints as a result of step 1. Post-process all TCs by validating against properties (ii) and (iii) to find valid TCs. Condition (iii), i.e. the condition on minimum size of TCs, is relaxed for those positive values with TCs having no possibility of increasing their size, such as there is no positive nodes at the lower levels. This relaxation is to increase the ultimate pit value.

Step 7. Qualified TCs from step 6 are removed from the block model while nodes of unqualified TCs remain.

Step 8. If the current search level reaches the bottom, the algorithm moves to step 9, otherwise all variables are reset and the algorithm moves to a lower level and back to step 1.

Step 9. Print the results and stop.

**Features of TCA**

- The TCA generates TCs by scanning through all ore blocks available in the orebody, from the top level to the bottom level, and sequentially extracts as many ore blocks, together with the overburden, as possible, given that each time of extraction generates economic profit. By definition, this process results in an ultimate pit.

- By varying the properties applied on TCs, particularly a minimum number of blocks per cone or MCS, the number of aggregates is controlled. Theoretically, the TCA can reduce the number of TCs to close to, or equal to, unity when MCS is set large enough. The smallest number can be unity only if the shape of the ultimate pit limit allows all blocks to be connected to a root block according to the slope constraints. That theoretical ultimate pit limit has a cone shape with an appropriate slope angle and an ore block located at the bottom. This critical ability of the TCA ensures that there is always an appropriate number of variables in the IP scheduling model to make it tractable.

- The TCA is able to incorporate various constraints into TCs via its post-processing step. At the current development stage, only the minimum size of cones is considered.

- TCA is a linear programming method, therefore the computation time required for generating TCs is not a major concern if sufficient memory is available. Together with feature 2 above, dictating the number of TCs being controlled, the proposed mine planning framework using TCA and IP can be applied to any large-scale data-sets and a solution obtained within a relatively short computation time.

**Demonstration of TCA in a 2D instance**

The TCA is demonstrated in a 2D example using a hypothetical cross-sectional view of a mineral deposit, represented by nodes. The numbers inside circles (Figure 4) represent node indexes, and those outside are economic values. The MCS comprises two nodes.

Step 1. Start from level 1, no positive node is found. Then, the algorithm moves to level 2 where there are four positive nodes, namely 9, 10, 11, and 12. They are now termed underlying nodes.

Step 2. Generate the network flow as shown in Figure 4.

Steps 3, 4. The cone values of the four underlying nodes are -7, -5, -11, and +3. Therefore, their coefficients are 3, 2, 4, and 1, respectively.

Step 5. Formulate the LP model of level 2 (Figure 5) The solution is presented in Table I and Figure 6, showing positive flows only.
A new open-pit mine planning optimization method using block aggregation and integer programming

Table I

<table>
<thead>
<tr>
<th>Flow</th>
<th>fs9</th>
<th>fs10</th>
<th>fs12</th>
<th>f1t</th>
<th>f2t</th>
<th>f3t</th>
<th>f4t</th>
<th>f5t</th>
<th>f6t</th>
<th>f9.1</th>
<th>f9.3</th>
<th>f10.2</th>
<th>f10.3</th>
<th>f12.4</th>
<th>f12.5</th>
<th>f12.6</th>
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</thead>
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<td>8.00</td>
<td>10.03</td>
<td>1.01</td>
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<td>3.01</td>
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<td>5.01</td>
<td>2.99</td>
<td>3.01</td>
<td>4.01</td>
<td>3.01</td>
<td></td>
</tr>
</tbody>
</table>

Figure 4—Representation of network flow of level 2

Figure 5—LP formulation of level 2

Figure 6—Representation of TopCones for level 2

Figure 7—Representation of network flow of level 3

Figure 8—LP formulation of level 3
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Step 6. Two TCs are found based on the LP solution, and they are valid according to the predefined properties. Considering precedence, TC 2 is removed after TC 1 to ensure slope safety.

Steps 7, 8. Mine out two TCs found at step 6, go to level 3, and similarly repeat steps 1–8 to find more TCs. The network flow corresponding LP formulation and result of level 3 are presented in Figures 7, 8, and 9 and Table II, respectively. Note that the value +1 of positive overlying node 11 is transferred to underlying node 18 because it has a smaller coefficient than underlying node 19.

Step 9. As level 3 is the last level, the algorithm stops. All TCs found are presented in Figure 10.

Besides satisfying all four properties, other comments on TCs are as follows.

- TC 3 only consists of two positive nodes.
- The combination of all four TCs forms an ultimate pit limit.
- In TC 2, two positive nodes 9 and 10 have jointly supported three negative nodes 1, 2, and 3.
- When mining TCs follow their order of appearance, the slope constraints are always secured. This feature significantly reduces the number of sequencing constraints at IP formulation.
- At level 3, the combination of node 11 with node 18 instead of node 19 to form cones provides the most beneficial scenario for scheduling in terms of NPV. This demonstrates the important role of coefficients in the LP formulation as discussed in step 5 of the TCA (setting up and solving the LP formulation).

After generating TCs, the long-term production scheduling using the IP model is implemented using the formulation presented in the next section.

Optimization of long-term production scheduling using TopCones

In this section we discuss an open pit mine long-term production scheduling model using integer programming and TopCones. In the model, each TC can be taken at any scheduling period over the life of mine to maintain a global optimization. A TC’s ore tonnage, waste tonnage, and economic value are calculated by summing from the constituting blocks, while the TC’s grade is assumed to be homogeneous within the ore tonnage and calculated by averaging the grade of the TC’s ore material. As the whole TC will be extracted at once when it is scheduled in a period, the assumption of homogeneity has no impact on the scheduling result.

| Table II |
|---|---|---|---|---|---|---|---|---|---|---|
| Flow | f18 | f19 | f7t | f11t | f13t | f18.11 | f19.7 | f19.13 |
| Value | 0.01 | 7.02 | 2.01 | 0.01 | 5.01 | 0.01 | 2.01 | 5.01 |

Figure 9—Representation of TopCones for level 3

![Figure 9](image)

Figure 10—Final result of TopCones and the ultimate pit limit

![Figure 10](image)
A new open-pit mine planning optimization method using block aggregation and integer programming

Indices and sets

- $i \in N$: Set of TCs $i$
- $j \in M, \subset N$: Subset of TCs $j$ that are predecessor TCs for TC $i$
- $t, t' \in P$: Set of time periods $t, t'$ in the horizon

Parameters

- $d$: Economic discount rate
- $V_i$: Economic value of TC $i$
- $C_i^t$: Expected value of TC $i$ when being extracted in period $t$

\[
C_i^t = \frac{1}{(1 + d)^t} V_i \quad [8]
\]

- $G_i$: Grade of TC $i$
- $O_i$: Ore tonnage of TC $i$
- $W_i$: Waste tonnage of TC $i$
- $G_{mi}/G_{ma}$: Minimum/maximum blending grade
- $PC_{mi}/PC_{ma}$: Minimum/maximum processing plant capacity
- $MC_{mi}/MC_{ma}$: Minimum/maximum capacity of the mine’s equipment.

Binary variables

- $X_i^t$: equal to 1 if TC $i$ is scheduled in period $t$; 0 otherwise.

Objective function

\[
\sum_{t \in P} \sum_{i \in N} C_i^t \cdot X_i^t \quad [9]
\]

Subject to

\[
\sum_{i \in N} (G_i - G_{ma}) O_i \cdot X_i^t \leq 0 \quad \forall t \in P \quad [10]
\]

\[
\sum_{i \in N} (G_i - G_{mi}) O_i \cdot X_i^t \geq 0 \quad \forall t \in P \quad [11]
\]

\[
\sum_{i \in N} X_i^t \leq 1 \quad \forall i \in N \quad [12]
\]

\[
\sum_{i \in N} (O_i, x_i^t) \leq PC_{ma} \quad \forall t \in P \quad [13]
\]

\[
\sum_{i \in N} (O_i, x_i^t) \geq PC_{mi} \quad \forall t \in P \quad [14]
\]

\[
\sum_{i \in N} (O_i + W_i) X_i^t \leq MC_{ma} \quad \forall t \in P \quad [15]
\]

\[
\sum_{i \in N} (O_i + W_i) X_i^t \geq MC_{mi} \quad \forall t \in P \quad [16]
\]

\[
M_i \cdot X_i^t - \sum_{j \in M_i, t \in E} X_j^t \leq 0 \quad \forall i \in N, \forall t \in P \quad [17]
\]

$X_i^t = \{0,1\}$ and $x_i^t = \{0,1\}$ \forall i, j \in N, \forall t, t' \in P \quad [18]

Numerical results

The two-phase mine planning method using TCA and IP proposed in this paper was applied to a hypothetical copper deposit. The block model characterizing the deposit consists of 1,568,250 blocks of size 20×20×20 m, 20,571 of which are ore blocks. The experiments were implemented using a normal office computer with an Intel® Core™ i7 with 3.4 GHz CPU processor and 8 GB of RAM. The hypothetical set of scheduling targets is presented in Table III.

In this study, the proposed mine planning method was implemented multiple times using seven MCS options of 1, 10, 75, 100, 200, and 300 blocks per TopCone. The MCS of 1 implies that there is no restriction on the minimum size of the TCs. The ultimate pit limit and production scheduling solutions yielded by the proposed method were validated against the Whittle Milawa NPV algorithm (Whittle, 2016). Those blocks inside the optimal Whittle ultimate pit limit are regarded as the original, non-aggregated problem. Table IV shows the results of the numerical experiments. For each run, the following parameters are reported:

- Number of TCs generated by TCA or blocks in the original problem
- Number of binary variables: Equal to number of TCs or blocks multiplied by number of time periods
- Number of precedences: Number of precedence arcs between TCs.
- Number of constraints: Include resource constraints, maximum processing capacity constraints, and slope constraints, as formulated by constraints [12], [13], and [17] respectively
- Gap to optimum ultimate pit (%): The ultimate pit values encapsulated by TCs are compared with the optimum solution
- NPV gap (%): The NPV obtained from solving TCs-based IP models is compared with the block-based Whittle Milawa NPV algorithm
- Solution time of TCA (minutes): The computing time of TCA in aggregating blocks into TCs
- Solution time of IP (minutes): The computing time of solving IP models built on TCs. CPLEX was used as the solver using standard parameters and an optimality tolerance gap of 5%.
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As demonstrated in Table IV, the solution time for solving IP models increases exponentially with the number of TCs, and this emphasizes the significance of controlling the number of aggregates while clustering blocks. Specifically, the aggregated problems are orders of magnitude (in the thousands) smaller than the original block-based problem. For instance, at a MCS of 300, only 239 TCs are generated together with 1912 binary variables, 483 precedences, and 2159 constraints for the corresponding IP model, a staggering reduction from the 562,800 binary variables of the original problem. Consequently, the whole two-stage mine planning process with a MCS of 300 was completed in just seven minutes. Considering the experiments were conducted with limited computing power, that time-frame is very practical. In the scenarios of MCS of 1 and 10, CPLEX did not obtain a solution within five days, so the experiments were terminated as this is no longer a practical time-frame.

The ability to obtain the ultimate pit limit using TCA was also validated in Table IV. Although a loss of 0.2 to 0.3% of pit value is relatively small, TCA can be implemented within a predefined optimal pit limit to completely eliminate this issue. Hence the experiments presented in this study are aimed at demonstrating the ability of TCA to find near-optimal pit limits only.

Figures 11 and 12 present detailed comparisons of the TCA-IP model with MCS of 300 and Whittle Milawa NPV’s scheduling scenarios regarding tonnage production, NPV, and blending grade. To maximize NPV as defined in the objective function of the IP model, the proposed scheduling model maximizes the ore tonnage and copper grade while minimizing waste production in early periods. This explains the considerable improvement in NPV over the Whittle Milawa NPV’s solution.

To illustrate the practicality of the solutions of the proposed model, the plan view and typical cross-sections of the production schedule generated by TCA-based IP model with MCS of 300 are presented in Figure 13.

Conclusions

A new optimization method for open pit mine planning has been introduced, expounding the idea of aggregating mining blocks using a TopCone algorithm and then deploying integer programming to find production scheduling solutions based on aggregated blocks. By clustering blocks in a controlled manner regarding both quantity and shape of aggregates, TCA provides a novel platform for the application of mathematical programming techniques to solve mine planning problems. The numerical experiments demonstrated that the proposed method could solve large-scale problems quickly, even with limited computing power. In addition, it is suggested that determining pushbacks is not necessary while a near-optimal ultimate pit can be obtained during block aggregation. These features make the proposed mine planning framework simpler and easier to implement than conventional approaches.
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One limit of TCA is that some TCs become too large when the MCS is set too high, particularly those TCs generated near the pit bottom. These oversize TCs may reduce the flexibility of the IP scheduling model or even make it infeasible to solve, depending on the narrowness of the lower and upper bound constraints. In future work, we plan to mitigate the effect of oversize TCs via a post-processing step, such as by introducing a maximum cone size parameter or developing a MIP model for partial mining of TCs. Furthermore, incorporating a minimum width for mining benches would also be an interesting exercise.
A new open-pit mine planning optimization method using block aggregation and integer programming

References


Investigation of secondary zinc oxides as an alternative feed to the Skorpion Zinc process: Part 2 – Process considerations and economic analysis

by C. Lottering*† and C. Dorfling†

Synopsis

Skorpion Zinc is investigating the possibility of using secondary zinc oxides as an alternative feed to supplement the zinc oxide ore feed and to extend the life of mine. Part 1 of this communication provides the technical background on the leaching performance at the typical Skorpion Zinc operating conditions. This study reports on the process modelling and economic analysis that were performed to determine appropriate feed blending strategies for electric arc furnace (EAF) dust, zinc dross, and zinc fume dust based on process limitations and economic considerations.

The zinc fume dust had the highest zinc and lowest impurity content of the alternative oxide sources investigated; as a result, this alternative source resulted in the highest zinc production and profitability. At a blending ratio of 50% zinc fume in the solids feed, more than three times the current zinc production from ore could theoretically be achieved. Production from the zinc dross samples was limited by the amount of contained nickel; the maximum production was achieved at a blending ratio of 10% and was 20% higher than the current production from ore. Zinc production from EAF dust was very low at blending ratios exceeding 30%, due to Mg and Mn impurity limitations as well as the relatively low zinc content. Both zinc dross and EAF dust can also be processed economically to yield profit for Skorpion Zinc and the alternative oxide suppliers. Zinc dross was generally more profitable to process than EAF dust, despite its higher freight costs.

Keywords

leaching, process economics, secondary zinc oxide.

Introduction

Part 1 of this communication (Lottering and Dorfling, 2018) presented background on the Skorpion Zinc operation as well as a motivation for the work performed. In addition, results were presented on the leaching of alternative zinc oxide sources at conditions compatible with the current Skorpion Zinc operation. The dissolution of zinc and impurities such as Al and Fe was evaluated at temperatures between 40 and 70°C, and pH values between 1.2 and 2.1. It was concluded that electric arc furnace (EAF) dust, zinc dross, and zinc fume could all potentially be used to supplement the ore feed given the high extent of zinc dissolution and acceptably low acid consumption at the current Skorpion Zinc operating conditions.

In this part of the communication, the focus is therefore on determining the maximum amount of different alternative zinc oxides that could be fed to the plant considering operational limitations, and on determining the most economically feasible manner in which to supplement ore with alternative zinc oxide sources. Mass balances were performed taking into account the capacity of the respective unit operations in order to determine appropriate ore and alternative zinc oxide blending ratios. The mass balances were used as the basis to calculate production rates and operating costs, and for the associated economic analysis to compare the profitability of the different alternative zinc oxide sources and blending ratios. For the purpose of the mass balances and economic analysis, the leaching performance of the alternative zinc oxides at the current standard Skorpion Zinc operating conditions (pH 1.8 and 50°C) was used.

Skorpion Zinc process overview

Process description

The basic flow diagram of the Skorpion Zinc process is presented in Figure 1. Zinc ore from the open pit mine is crushed and milled to a $d_{50}$ particle size of 180 μm. Atmospheric sulphuric acid leaching of the ore takes place in a continuous operation consisting of five agitated leaching tanks arranged in series with a total residence time of approximately 2 hours. The slurry that leaves the ball mill contains 20% solids and flows into the first tank, where it is mixed with raffinate from the solvent extraction section. A small amount of pure acid is also added to adjust the pH to the required set-point for this tank. As the slurry flows into each subsequent leach tank, additional raffinate and pure acid are added to adjust the pH. The temperature in each tank is controlled to 50°C and the final pH at the end of the leaching section is maintained between 1.8 and 2 to maximize colloidal silica stability (Gnoinski, 2007). The main leaching reactions taking place have been presented in part 1 of this communication.

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† Department of Process Engineering, University of Stellenbosch, South Africa.
The pregnant leach solution (PLS) contains approximately 30 g/L zinc and high concentrations of impurities. The leaching step is followed by a neutralization step in which the pH of the leach solution is raised to 4 by calcium carbonate addition; this results in the agglomeration and precipitation of some of the major impurities, such as iron, aluminium, and silica.

The solids recovered after neutralization and thickening are re-acidified to recover any zinc that co-precipitated; this slurry is then filtered and the solids residue disposed of as tailings. The liquid filtrate is treated with milk of lime to precipitate the zinc in solution as a basic zinc sulphate, which is recycled to the neutralization section of the plant.

Approximately 25% of the PLS recovered from the thickener following neutralization is treated with zinc dust to precipitate Cu and Ni before being recycled to the process feed. The remaining 75% of the PLS is clarified before being sent to solvent extraction, where the zinc tenor is increased from approximately 30 g/L to 120 g/L and many of the impurities in the PLS are removed. Removal of the dissolved halides from the solution is of crucial interest in this section (Gnoinski, 2007). These impurities cause major corrosion of the lead anodes used in the electrowinning section of the process, which subsequently leads to unacceptable high lead levels in the electrolyte solution and in the plated zinc.

The electrowinning process used by Skorpion Zinc to recover elemental zinc from the electrolyte is sensitive to the presence of impurities. It is therefore very important to ensure that the solvent extraction circuit can deal with the impurities introduced with the leach solution generated when supplementing the feed with alternative zinc oxides. Currently, Skorpion Zinc aims to obtain a PLS after neutralization with impurity concentrations as listed in Table I.

Since the electrowinning circuit regenerates the acid consumed for zinc leaching, the real net acid consumption is determined by the acid consumed as a result of gangue leaching as well as acid lost to the residue tailings. The main acid-consuming elements in the Skorpion Zinc circuit are calcium (as calcite), iron, and manganese. These elements are found in their oxide forms in the ore, and exist in similar forms in the secondary zinc oxides.

<table>
<thead>
<tr>
<th>Element</th>
<th>Concentration range</th>
<th>Element</th>
<th>Concentration range</th>
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<tr>
<td>Zn (g/L)</td>
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<td>Cu (g/L)</td>
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</tr>
<tr>
<td>Al (g/L)</td>
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<td>Co (g/L)</td>
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<td>Si (ppm)</td>
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<td>&lt; 0.5</td>
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<td>&lt; 3</td>
<td>Cl (g/L)</td>
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</tr>
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</table>
**Effects of impurities on leaching and downstream processing**

Certain elements associated with secondary zinc oxides are not present in the Skorpion Zinc ore. For this reason, there are currently no limits set on the concentrations of these impurities that the solvent extraction and electrowinning sections are capable of treating. The possible effects of the different impurities on solvent extraction and electrowinning are reviewed briefly to demonstrate the importance of effective impurity control in the process.

Aluminium in the electrolyte causes changes to the morphology of the zinc cathode, leading to larger grain sizes and therefore a coarser cathode deposit (Fukubayashi, 1972). In addition, aluminium can form complexes with fluorides, which would allow these ions to be transported to the electrowinning circuit where they damage the surface of the aluminium cathodes.

A high calcium content in the leach solution is an indication that a large amount of calcium has been leached, which causes excessive acid consumption in the leaching section. A large amount of calcium (> 4%) in the ore fed to the leaching section has previously caused the Skorpion Zinc leaching tanks to overflow due to gas generated by the reaction between calcium species and sulphuric acid. Most of the calcium in process solutions is removed in the neutralization step, so that the calcium can be removed from the circuit via the residue solids or tailings. Calcium concentrations exceeding 500 ppm in the PLS can lead to scaling of process equipment. This can be particularly detrimental in solvent extraction and electrowinning, where equipment must be taken offline to be cleaned. With the sensitivity of both these sections to process upsets, it is desirable to limit the calcium concentration to a maximum of 500 ppm.

The presence of nickel in the cellhouse electrolyte in quantities exceeding 200 ppb can cause dissolution of the plated zinc and decrease the current efficiency (Fukubayashi, 1972). Dissolution can cause large quantities of hydrogen to be emitted, which presents a serious fire and explosion risk in a zinc tankhouse. Copper, on the other hand, has a lower overpotential than zinc and would therefore plate preferentially to zinc. In addition, hydrogen will be released and the current efficiency reduced. Carryover of copper to the electrolyte can occur via entrainment of PLS in the loaded organic phase across solvent extraction. The copper cementation process treats a bleed stream of the PLS to prevent accumulation of copper in the leaching circuit.

Iron present in the leach solution entering the solvent extraction circuit will be preferentially extracted along with zinc. Unlike zinc, however, iron cannot be easily stripped off the organic phase by the spent electrolyte. This means that the iron accumulates in the organic, limiting the amount of extractant available for zinc to bind onto. For this reason, an organic stream is bled from the storage tank and contacted with 6 M HCl, which strips the iron from the organic (Alberts and Dorfling, 2013). During this process, the HCl becomes deactivated and must be regenerated in the Skorpion Zinc HCl plant. Iron from the HCl plant is bled from the process via a small bleed to the effluent treatment plant. In addition, any iron that enters the electrowinning circuit will reduce the current efficiency by continuously being oxidized to ferric iron at the anode, and then being reduced to ferrous iron at the cathode (Schlesinger et al., 2011).

Although the electrolyte entering the electrowinning circuit seldom contains large quantities of lead, lead-silver anodes are used for the electroplating process. Corrosion of these anodes by the highly acidic electrolyte solution introduces significant quantities of lead into the electrolyte in the form of PbSO₄. This lead phase is very sparingly soluble, forming a fine precipitate that remains in suspension in the solution. During the plating process, the movement of zinc ions to the aluminium cathode traps the lead sulphate particles in the plated zinc matrix. The zinc that Skorpion Zinc aims to produce is classified as Special High Grade, with the specification that the zinc product should contain no more than 50 ppm total impurities, consisting of no more than 30 ppm lead, 10 ppm copper, and 20 ppm iron, as well as small quantities of other metal impurities. Thus, the entrainment of lead in the plated zinc matrix results in zinc that does not conform to the Special High Grade specifications. This will result in reduced profit for the company. Lead content in the feed solids to the leaching process should, however, not be a concern for electrowinning, as the lead should not be transferred across the solvent extraction circuit, and will therefore be removed from the process in the residue tailings and bleed streams.

Magnesium increases electrolyte viscosity and density, limiting the maximum attainable zinc concentration and raising the cell voltage. Manganese, although beneficial because it helps form a protective layer on the lead anode, also limits the maximum zinc content in the electrolyte (Sinclair, 2005). These elements are removed from the process via the tailings or residue stream, in both the solids and the liquids, as well as via the effluent treatment bleed stream.

When silica is dissolved as orthosilicic acid (H₄SiO₄) during leaching, the silica in solution may cause problems in the solvent extraction circuit. This form of silica is unstable in aqueous solutions and will decompose in acidic aqueous solutions (Moradi and Monhemius, 2011). The silica formed will precipitate as a polymer which can form hydrated gels, which cause problems with phase separation in the solvent extraction circuit (Sinclair, 2005).

**Mass balance**

**Approach and assumptions**

To determine the conditions under which maximum production could be achieved, the results from the experimental test work were used to perform mass balances for different blends of Skorpion Zinc ore and each of the alternative oxides. For each of the alternative oxides, blends of 10, 20, 30, 40, and 50% oxide with the ore were considered. It was assumed that the Skorpion Zinc ore grade remained constant at 9%. The compositions of the alternative zinc oxides were used as reported in part 1 of this communication. Based on historical plant data, the ore and limestone were specified to have a moisture content of 5%; the same moisture content was assumed for the alternative oxides.

Dissolution of the different elements contained in the Skorpion Zinc ore was estimated based on historical plant data.
data. Dissolution of the elements from the alternative oxides was determined from the experimental work presented in part 1 of this communication. A summary of the metal dissolutions achieved from the different sources at the typical Skorpion Zinc operating conditions is presented in Table II.

The key limiting factor for the different alternative zinc oxide sources was the impurity build-up in the Skorpion Zinc process. Calcium, aluminium, iron, silica, copper, nickel, manganese, and magnesium were the main impurities considered given their potential impact on the downstream unit operations. Calcium, silica, iron, and aluminium are normally completely removed from the circuit with the

<table>
<thead>
<tr>
<th></th>
<th>SZ ore (%)</th>
<th>EAF dust (%)</th>
<th>Zinc dross (%)</th>
<th>Zinc fume (%)</th>
</tr>
</thead>
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<tr>
<td>Zn</td>
<td>89</td>
<td>93</td>
<td>97</td>
<td>99</td>
</tr>
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<tr>
<td>Mn</td>
<td>80</td>
<td>70</td>
<td>73</td>
<td>83</td>
</tr>
</tbody>
</table>

Table II

Extents of metal dissolution achieved from Skorpion Zinc ore and the respective alternative zinc oxide sources at the current operating conditions

Figure 2—Skorpion Zinc process flow sheet with mass balance system boundary (flow sheet adapted from Gnoinski, 2007)

Figure 3—Schematic overview of the mass balance strategy followed to determine the maximum solids feed rate for a given ore/alternative oxides blending ratio. The subscript RM refers to residue moisture
tailings, due to the combination of the neutralization section, effluent treatment, and the bleed stream from the solvent extraction circuit.

The system around which the mass balance was performed is shown in Figure 2. Sulphuric acid addition was calculated based on the Skorpion Zinc design target of 1.5 t per ton of zinc for ore, together with the experimentally determined acid consumptions for the oxides under investigation (0.11, 1.02, and 1.34 tons per ton for zinc fume, EAF dust, and zinc dross, respectively). Limestone addition was estimated based on actual plant data at approximately 16% by mass of the total dry solids feed to the refinery. It was assumed that a similar limestone addition would be used when blending alternative zinc oxides as supplementary feed material.

The mass balance was performed by specifying the desired feed ratio, whereafter the amount of oxide that could be fed under a certain set of conditions was determined by iteratively adjusting the amount of solids fed to the system until one of the impurities started to accumulate in the system. If impurity accumulation or removal was not found to be limiting, the total feed rate was set to the current maximum allowable feed rate of 230 t/h solids, and the plant throughput was then considered to be the limiting factor. A schematic overview of the mass balance approach is presented in Figure 3.

Assuming that aluminium, iron, silica, and calcium were completely removed in the neutralization section, the residue solids composition was based on a balance of these elements over the system. In the case of Mn and Mg, the residual solids were specified to have a fixed Mn and Mg content, and the Mn and Mg contents of the residue moisture were calculated by mass balance. The Skorpion Zinc design limited the Mn and Mg contents in the residue moisture to 4 g/L; these impurities would thus accumulate in the system if the Mn and/or Mg content of the residue moisture exceeded 4 g/L. The moisture content in the tailings was assumed to be 40%, based on the average historical moisture content of the tailings material produced by Skorpion Zinc.

Copper and nickel were balanced by calculating the required capacities of the copper and nickel removal sections. If the required capacity exceeded the actual maximum capacity of either of these sections, the feed rate of material was deemed to be limited by the copper or nickel removal capacity and the subsequent accumulation of these impurities that would occur at higher feed rates. Based on the plant design and actual operating data, the copper removal section was assumed to be able to handle a throughput of 250 m³/h at a copper concentration of 500 mg/L, resulting in a total copper removal of 125 kg/h. The assumptions for the nickel removal section were similarly based on plant experience, and resulted in an assumed capacity of 60 m³/h at 400 mg/L Ni, resulting in 24 kg/h nickel removal at maximum capacity.

**Results**

The base case mass balance for the Skorpion Zinc refinery assumed that a total of 230 t/h ore was fed to the circuit without any alternative zinc oxides supplementing the feed. This resulted in a total zinc production of 151 000 t/a in the mass balance calculation. Once the base case had been established, each of the alternative oxides was run through the mass balance in different blending ratios with the Skorpion Zinc ore, and the maximum achievable zinc production for each scenario was calculated. These results are summarized in Table III, along with the limiting factor for each scenario.

<table>
<thead>
<tr>
<th>Oxide source</th>
<th>Ore feed (%)</th>
<th>Oxides feed (%)</th>
<th>Maximum total feed rate (t/h)</th>
<th>Maximum Zn production (t/h)</th>
<th>Limiting factor</th>
</tr>
</thead>
<tbody>
<tr>
<td>Base case</td>
<td>100</td>
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<td>230</td>
<td>19</td>
<td>Throughput</td>
</tr>
<tr>
<td>EAF dust</td>
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<td>Throughput</td>
</tr>
<tr>
<td>EAF dust</td>
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<td>20</td>
<td>230</td>
<td>28</td>
<td>Throughput</td>
</tr>
<tr>
<td>EAF dust</td>
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<td>192</td>
<td>27</td>
<td>Copper</td>
</tr>
<tr>
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<td>13</td>
<td>4</td>
<td>Magnesium</td>
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<td>50</td>
<td>3</td>
<td>2</td>
<td>Manganese</td>
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<tr>
<td>Zinc dross</td>
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<td>174</td>
<td>23</td>
<td>Nickel</td>
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<td>20</td>
<td>115</td>
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<td>Nickel</td>
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<td>69</td>
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<td>Nickel</td>
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<td>57</td>
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<td>230</td>
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<td>202</td>
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<td>Copper</td>
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<tr>
<td>Fume dust</td>
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<td>40</td>
<td>173</td>
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<td>Copper</td>
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<td>Fume dust</td>
<td>50</td>
<td>50</td>
<td>152</td>
<td>63</td>
<td>Copper</td>
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</table>
From Table III it is clear that the zinc fume dust provided the highest overall zinc throughput and production at all blending ratios. This is due to the combination of a high zinc content and low impurity levels in this material. Copper became the limiting impurity at the point where the solids feed contained 30% or more zinc fume oxide. Since this sample's production capacity was limited by the plant throughput in the two lowest proportion blends, and because it had a much higher zinc grade than the Skorpion ore, the zinc production decreased as the alternative oxide portion of the feed decreased.

Production from the zinc dross sample was limited by the amount of nickel contained in the sample at all the investigated blending ratios. This alternative oxide source contained the highest amount of nickel at 0.2%, which is an order of magnitude higher than the nickel content of the other two sources. This was most likely due to the type of steel used in the hot-dip galvanizing process. Zinc production from the zinc dross was fairly consistent for all blend scenarios, with an increase of only about 30 000 t per year in production across the range of blends tested.

EAF dust, despite containing the highest percentage of impurities and lowest zinc percentage, was not limited by its impurity content at ratios of 20% or less with the Skorpion Zinc ore. Instead, it was initially limited by the maximum plant throughput of 230 t/h solids feed. For solids blending ratios of 30% or more EAF dust, first copper, then magnesium, and finally manganese became limiting elements. Zinc production from EAF dust at these higher blending ratios was overall very low; this was partially due to high impurity contents, but also to the lower zinc content relative to the other alternative oxide sources investigated.

Thus, the optimum zinc production was achieved at 20% blend before impurity accumulation started to limit the maximum total throughput to the refinery.

From the mass balance, it was evident that the zinc fume dust provided the highest overall zinc production potential. For each of the oxides, there is a scenario that would provide the maximum potential zinc production. For EAF dust, a blending ratio of 20% with ore is ideal, while the dross should be blended in a 10% ratio. Zinc fume provides the highest theoretical zinc production at a 50% blend with Skorpion Zinc ore.

Other process considerations

The high iron and aluminium contents in the leach residue liquid generated from the EAF dust and zinc dross samples should be further investigated if the decision is made to use these samples as supplementary feed. Currently, the plant limits the allowable Fe and Al concentrations in the leach solution to 240 and 1450 mg/L, respectively. Leaching of only EAF dust would exceed the iron limit, while leaching of zinc dross would exceed both the Fe and Al limits.

Neutralization tests should therefore be performed to determine whether the Skorpion Zinc circuit would be capable of handling these impurities, to obtain a more accurate indication of the expected reagent consumption in this section of the plant, and to determine the residue composition and its potential environmental impact. This might place an upper limit on the allowable percentage of alternative oxides in the blended feed stream to prevent extreme impurity levels in the circuit. In addition, specialized waste management procedures and additional environmental permits may be required, which may have an impact on the economic feasibility of using the alternative oxides.

The potentially hazardous nature of the EAF dust and zinc dross is an important factor to consider when it comes to disposal of the tailings generated by leaching these solids. Harmful lead, chromium, and cobalt are generally leached together with the zinc (Oustadakis et al., 2010). In the case of the Skorpion Zinc process, most of these impurities will be removed together with the copper or nickel in their respective removal sections.

The zinc content in the PLS for each of the alternative zinc oxide samples exceeded the design maximum zinc tenor in the leach solution for the Skorpion Zinc circuit. Depending on which alternative oxide is chosen as supplementary feed and what blending ratios are used, some adjustments to the operating philosophy in the solvent extraction circuit may be required. Careful consideration will also have to be given to the metal accounting if the feed consists of a mixture of ore and alternative oxides. Attention will need to be given to determining how to account for the feed to the circuit, and how to account for the recoveries obtained from the respective materials, if these need to be accounted for separately.

Some additional studies can be done on the possibility of blending the alternative zinc oxides investigated in this study with the stockpiled marginal zinc ore from the Skorpion Zinc pit. This material contains less than 4% zinc and high (> 7%) calcium with large amounts of copper. Given the high zinc and low impurity content of the zinc fume, for example, it may be possible to economically utilize the marginal zinc ore with appropriate blending strategies to reduce the acid costs and impurity levels.

Economic analysis

The potential profit that Skorpion Zinc could make from processing each of the respective oxides was calculated for the different blending ratios using an assumed London Metal Exchange (LME) zinc price with a fixed premium for Special High Grade zinc. The assumed zinc price (including the premium) per unit mass of zinc was scaled to a value of 100, and all other calculations were performed and standardized relative to this scaled zinc price. Since there is no standard price for the purchase of alternative zinc oxides, it was assumed that a certain percentage of the LME zinc price would be paid for the zinc contained in the alternative oxides. Two different scenarios were investigated to determine the profitability of processing the different zinc oxides: purchase prices of 20% and 30% of LME zinc price were used.

In each scenario, the potential revenue that Skorpion Zinc could achieve from each oxide was calculated using the total theoretical zinc production as determined by the mass balance. Next, the total operating cost for zinc production was calculated using the actual costs from the Skorpion Zinc refinery and adjusting the costs for the volumes of oxide that would be fed. Adjustments were also made to the mining and comminution costs to account for the fact that no mining or comminution would be required for these alternative oxide feed sources. The last cost element on the Skorpion Zinc side,
Investigation of secondary zinc oxides as an alternative feed to the Skorpion Zinc process: Part 2

Table IV
Summary of the standardized annual overall income and expenditure for each blending ratio for the different alternative zinc oxide sources. Values are standardized relative to the assumed LME zinc price plus premium, which was scaled to a value of 100

<table>
<thead>
<tr>
<th>Blending ratio</th>
<th>Oxide source</th>
<th>Revenue from oxides (million)</th>
<th>Net cost of production (million)</th>
<th>% LME for zinc in oxide</th>
<th>Total purchase cost (million)</th>
<th>EBITDA (million)</th>
<th>Income from oxide sales (million)</th>
<th>Total freight (million)</th>
<th>Supplier profit (million)</th>
</tr>
</thead>
<tbody>
<tr>
<td>10%</td>
<td>EAF dust</td>
<td>4.81</td>
<td>2.44</td>
<td>20</td>
<td>1.02</td>
<td>1.35</td>
<td>1.02</td>
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<td>3.22</td>
<td>2.43</td>
<td>3.22</td>
<td>2.89</td>
<td>0.33</td>
</tr>
<tr>
<td>40%</td>
<td>EAF dust</td>
<td>1.09</td>
<td>0.55</td>
<td>30</td>
<td>0.34</td>
<td>0.19</td>
<td>0.34</td>
<td>0.27</td>
<td>0.08</td>
</tr>
<tr>
<td></td>
<td>Fume</td>
<td>37.67</td>
<td>16.28</td>
<td>30</td>
<td>11.19</td>
<td>10.21</td>
<td>11.19</td>
<td>3.35</td>
<td>7.84</td>
</tr>
<tr>
<td></td>
<td>Dross</td>
<td>11.47</td>
<td>5.46</td>
<td>30</td>
<td>3.43</td>
<td>2.58</td>
<td>3.43</td>
<td>3.08</td>
<td>0.35</td>
</tr>
<tr>
<td>50%</td>
<td>EAF dust</td>
<td>0.35</td>
<td>0.18</td>
<td>30</td>
<td>0.11</td>
<td>0.06</td>
<td>0.11</td>
<td>0.08</td>
<td>0.02</td>
</tr>
<tr>
<td></td>
<td>Fume</td>
<td>41.22</td>
<td>17.81</td>
<td>30</td>
<td>12.24</td>
<td>11.17</td>
<td>12.24</td>
<td>3.66</td>
<td>8.58</td>
</tr>
<tr>
<td></td>
<td>Dross</td>
<td>11.92</td>
<td>5.67</td>
<td>30</td>
<td>3.56</td>
<td>2.68</td>
<td>3.56</td>
<td>3.20</td>
<td>0.36</td>
</tr>
</tbody>
</table>

The cost of purchasing the oxide, was calculated based on the assumed LME percentages. Finally, the earnings before interest, tax deductions, and amortization (EBITDA) were calculated for each scenario.

It was important to ensure that the prices of the alternative zinc oxide sources used in the calculations were reasonable. To determine this, the supplier’s profit was also calculated based on the assumption that the supplier would be responsible for paying the freight costs to ship the alternative zinc oxide to the Skorpion Zinc refinery. Situations that resulted in a profit for both the supplier and Skorpion Zinc were considered plausible. A summary of the total profit for the supplier and Skorpion Zinc is shown in Table IV.

It is evident that if 20% of the LME zinc price is paid for the alternative zinc oxide sources, the suppliers of EAF dust and zinc dross will not make a profit even though Skorpion Zinc will. The zinc fume oxide source was the only plausible alternative oxide if suppliers are paid 20% of the LME zinc value, since this is the only source for which both the supplier and Skorpion Zinc would make a profit. If the prices of the alternative oxide sources are set at 30% of the LME zinc price, all parties will make a profit; however, Skorpion Zinc’s profit per ton is reduced by between 27 and 38%, depending on the source and blending ratio.

In general, for zinc fume and dross, the total income and profit increased as the percentage of alternative oxides in the feed increased. In the case of EAF dust, increasing the blending ratio to the point where the feed contained more than 30% alternative oxides significantly limited the total feed and the zinc production rate due to the accumulation of magnesium and manganese, which resulted in reduced EBITDA.

It is clear from these results that the zinc fume oxide is the most profitable alternative oxide source for Skorpion Zinc. This is because of the low impurity levels in this source, which allows large zinc production volumes to be generated. At a 50% blending ratio, Skorpion Zinc could
Investigation of secondary zinc oxides as an alternative feed to the Skorpion Zinc process: Part 2

The financial feasibility study performed using the mass balance results showed that the zinc fume oxide would be the most profitable option of the three alternative oxides. This was the only source that was profitable for both the supplier and Skorpion Zinc at a 20% LME zinc purchase price for the zinc contained in the oxide. Increasing the proposed purchase price to 30% of the LME zinc price to make it more attractive to the supplier resulted in a decrease in Skorpion Zinc’s profit of 27–38%, while the supplier’s profit increased by 90%.

Conclusions
This paper forms the second part of a two-part communication reporting on the evaluation of the technical and economic feasibility of using secondary zinc oxides as a supplementary feed to the Skorpion Zinc process. The leaching performance at the typical operating conditions currently used at Skorpion Zinc, as reported in part 1 of this communication, was used to perform mass balances and economic evaluations for the respective alternative zinc oxide sources and ore/alternative oxide blending ratios.

From the mass balance it was determined that the zinc fume sample would consistently provide the highest zinc production of the three samples, regardless of the blending ratio used. This material can theoretically lead to an annual zinc production of 495 kt when blended in a 50% ratio with the Skorpion Zinc ore. This is significantly more than the maximum production achievable when using EAF dust (220 kt/a at a 20% blending ratio) or zinc dross (185 kt/a at a ratio of 10%) as alternative oxide sources. This was due, in part, to the high zinc content of the zinc fume and its low impurity content, which resulted in low acid consumption and hence reduced operating costs. At a blending ratio of 30% or less, zinc production from EAF dust exceeded that from zinc dross, despite its lower zinc content. Production from zinc dross was limited to a large extent by its impurity levels and, more specifically, the nickel content, which was higher than that in either of the other two sources.

Impurity levels in the PLS produced from EAF dust and zinc dross may be a cause for concern. Leaching of EAF dust and zinc dross resulted in aluminium and iron concentrations in the PLS exceeding the original Skorpion Zinc circuit design specification limits of 1450 and 240 ppm, respectively. Appropriate blending ratios would thus be important to avoid overloading of the neutralization circuit, and further tests would be required to confirm the reagent requirements and precipitate characteristics. The operating philosophy for downstream units such as solvent extraction and electrowinning will also have to be evaluated for higher zinc tenors and production rates.

The authors gratefully acknowledge Skorpion Zinc for the supply of materials and financial support.

References


Draw control strategies in sublevel caving mines – A baseline mapping of LKAB’s Malmberget and Kiirunavaara mines

by G. Shekhar*, A. Gustafson*, P. Boeg-Jensen†, L. Malmgren†, and H. Schunnesson*

Synopsis
The Malmberget and Kiirunavaara mines are the two largest underground iron ore operations in the world. Luossavaara-Kiirunavaara AB (LKAB) uses sublevel caving (SLC) to operate the mines while maintaining a high level of productivity and safety. The paper enumerates the loading criteria and loading constraints at the mines and outlines details of mine design, layout, and geology affecting the draw control. A study of the various draw control strategies used in sublevel caving operations globally has also been done to establish the present state-of-the-art. An analysis of the draw control and loading operations at the Malmberget and Kiirunavaara mines is summarized using information collected through interviews, internal documents, meetings, and manuals. An optimized draw control strategy is vital for improving ore recovery and reducing dilution in SLC. Based on the literature review and baseline mapping study, a set of guidelines for designing a new draw control strategy is presented. The draw control strategy at Malmberget and Kiirunavaara is guided by a bucket-weight-based drawpoint monitoring system that is part of the overall framework. Both mines employ a draw control strategy that considers the production requirements and mining constraints while regulating the loading process through an empirical method based on bucket weights and grades. However, in the present scenario of fluctuating metal prices and increasing operational costs a new draw control strategy is needed which is probabilistic in nature and can handle the uncertainties associated with caving operations.

Keywords
sublevel caving, draw control, optimization, drawpoint monitoring.

Introduction
A consistent issue in sublevel caving (SLC) operations is the regulation of loading at the drawpoints, especially as the decision to stop loading from a particular drawpoint and blast the next ring is irreversible. In caving operations, the draw control regulates loading by providing information on when to stop loading (Shekhar, Gustafson, and Schunnesson, 2016).

Smith and Rahal (2001) state that draw control incorporates the sequencing and scheduling of development, production, and the material handling system, with the dual (and contradictory) objectives of minimizing mining costs and dilution. Similarly, Bull and Page (2000) define an effective draw control strategy as one that maximizes ore recovery while minimizing dilution and delaying dilution entry in the drawpoint by deploying ‘corrective methods’. The contradictory objectives of reducing dilution while improving ore recovery are difficult to understand and complicated to apply. In SLC, the five parameters used to measure drawpoint performance are drawpoint dimensions, final extraction ratio, loading stoppage issues, fragmentation, and ore grade (Shekhar et al., 2016). Fragmentation is difficult to measure continuously, while drawpoint dimensions tend to remain constant. Therefore the remaining parameters – final extraction ratio, ore grades, and stoppage issues are vital to an analysis of drawpoint performance in an operating mine. Previous work cited in the literature and other results from physical models, marker trials, and field data analysis have identified seven factors of SLC which affect draw control; these are discussed in Table I. The paper presents the findings of a detailed baseline mapping study performed on the two largest underground iron ore operations, Malmberget and Kiirunavaara mines. The various draw control strategies used around the world are used to create a set of guidelines for a new draw control strategy.

Monitoring the drawpoint in SLC
The monitoring of the ore grade at the drawpoint is termed ‘grade control’ (Booth et al., 2004). The purpose is to provide information about the ore grade being loaded. The system used to monitor the ore grade depends on the ore type and mine requirements. Systems can provide ore grade measurements continuously (Quinteiro, Larsson, and Hustrulid, 2001) or periodically (e.g. every 100 t or based on work shifts) (Cokayne, 1982; McMurray, 1982). Drawpoint monitoring systems can be divided into three types based on the technique used.
Draw control strategies in sublevel caving mines

Table 1
Factors influencing draw control strategy

<table>
<thead>
<tr>
<th>Factor</th>
<th>Description</th>
<th>Effect on draw control</th>
</tr>
</thead>
<tbody>
<tr>
<td>Production planning</td>
<td>Scheduling and sequencing of the mine operation.</td>
<td>Draw control decides the amount of material to be loaded from each drawpoint, and production scheduling uses it as an input parameter (Jamieson, 2012; Villa and Diering, 2010).</td>
</tr>
<tr>
<td>Average ore grade and cut-off grades</td>
<td>Average ore grade is the overall target grade for the run of mine and is different from cut-off grade. Grade at the drawpoint when loading should be stopped is cut-off grade.</td>
<td>Changes in average grades and cut-off grades change the amount of material to be loaded from a drawpoint.</td>
</tr>
<tr>
<td>Dilution entry and total dilution</td>
<td>The introduction of the first dilution at drawpoint is called dilution entry. The total amount of waste loaded is total dilution.</td>
<td>Recent marker trials (Burton, 2009; Power, 2004; Wimmer et al., 2015) show that draw control is independent of dilution entry but an efficient draw control should aim at reducing total dilution (Laubscher, 2000).</td>
</tr>
<tr>
<td>Mine design (including ring design) and mine layout</td>
<td>Development cost and pillar stability must be balanced while increasing the drift spacing and other mine dimensions for safe operation (Bull and Page, 2000).</td>
<td>Draw control is generally adapted for a specific mine design and layout and is changed when mine design and layout change.</td>
</tr>
<tr>
<td>Ore geology and ore geometry</td>
<td>For SLC a stronger and competent ore is generally preferred (Bull and Page, 2000).</td>
<td>Draw control is sensitive to ore geometry; e.g. the nature of loading near the ore boundaries should be different to increase ore recovery and improve safety (Shekhar et al., 2016).</td>
</tr>
<tr>
<td>Performance of proximate drawpoints</td>
<td>Marker trials have shown that flow of material at the drawpoint can come from upper levels; i.e., secondary and tertiary recovery (Brunton, 2009; Power, 2004).</td>
<td>Draw control strategies have been modified to include these results when calibrating recovery models for loading at the drawpoint (Jamieson, 2012; Power and Campbell, 2016).</td>
</tr>
</tbody>
</table>

Visual estimation technique: This technique is based on the visual differentiation of ore and waste (Cokayne, 1982). The percentage of ore and waste present in the muckpile is visually estimated based on the colour, shape, or texture of the material (Booth et al., 2004). According to Booth et al., (2004), discrimination between ore and waste at Perseverance mine in Australia was based on rock colour and angularity. Lighter and harder hangingwall gneisses and rounded ultramafic footwall were easily distinguished from the darker and more angular ultramafic ore. McMurray (1982) describes a visual inspection used on a shift basis for the grade control of asbestos ore at Shabanie mine in Zimbabwe. Similar practices have been described for Craigmont mine, Canada (Baase, Diment, and Petrina, 1982) and the Frood-Stobie Complex, Canada (Nickson et al., 2012).

Sampling and assay system: This technique is used when no visual differences can be seen between ore and waste (Cokayne, 1982). Several samples are taken at the drawpoint and then prepared and analysed underground (Cokayne, 1982). Loading from the muckpile should be stopped during the sampling period in order to ensure a representative sample. Cokayne (1982) concludes that this method is more accurate than visual estimation. It is, however, very expensive, and is more time-consuming than visual estimation. Hancock and Mattson (1982) describe the use of this method for a chalcopyrite orebody at Granduc mine in Canada. Samples of 1.4 to 2.5 kg each were collected after each blast at intervals of every 20 to 30 scoop buckets until cut-off was reached. Laboratories were set up close to the centres of production, returning assay results within half an hour of sampling using standard sample preparation equipment and a portable isotope fluorescence X-ray analyser (Hancock and Mattson, 1982). More recently, bulk sampling has been done at Perseverance mine, albeit on rare occasions (Booth et al., 2004).

Density-based monitoring system: The density-based method is used when there is a significant difference in the density of ore and waste. The drawpoint monitoring method used in Kiirunavaara and Malmerberget mines exploits the difference in the density between ore (4.6–4.8 t/m³) and waste (2.7–2.8 t/m³) (Davison, 1996). A load cell placed on the hydraulic cylinder of the load haul dump (LHD) bucket reads the hydraulic pressure in the cylinder and converts it into tonnage. This weight measurement system is called the Loadrite scoop weighing system. The bucket weight reading is used to estimate the percentages of ore and waste in the bucket (visualized in Figure 1) (Gustafson et al., 2013; Klemo, 2005). The weights of 100% ore and 100% waste for completely filled buckets are known for different bucket sizes. WOLIS (Wireless Loader Information System) uses the bucket weight together with an assumed theoretical volume of the loaded material to calculate the grade of the material in each fully loaded bucket, i.e. the percentages of iron and waste present in the bucket. The formula for iron percentage used by WOLIS is given in Equation [1]:

\[
\text{Fe} \% = \left( \frac{(\text{Bucket weight} - X) + (X - Y)}{X} \right) \times 0.71
\]

where \(X\) is the weight of a bucket completely filled with ore and \(Y\) is the weight of the bucket completely filled with waste for a given LHD machine (Shekhar, Gustafson, and Schunnesson, 2017). As mines generally have machines with different bucket sizes, the WOLIS system uses different \(X\) and \(Y\) values stored for the different machines. Figure 2 shows one example of the details displayed to the LHD operator by WOLIS.
The operator can see information related to ring location (1), last bucket weight (2), ring design (3), extraction ratio (ratio of the total tonnage of material loaded from the drawpoint to the estimated tonnage of the ring) (4), system status (5), report status (6), loading graph (7), nearby charged holes (8), ring information (9), and extraction ratio of proximate rings (10). The operator can change the visualized graph if he or she wants to see, for example, iron ore content, extraction ratio, waste rock content, bucket number, tonnage. The information is used by the operator to decide if loading should be continued or discontinued at a particular drawpoint (Shekhar, Gustafson, and Schunnnesson, 2017).

The Malmberget and Kirunavaara mines have been storing comprehensive production data in a database management system for at least the last 10 years. The production data includes bucket weights, estimated bucket grades, and final extraction ratio for all the production rings blasted at the mines. This data can be used to calculate the average grade of a mined ring and observe the variation in ore flow throughout the loading process. Shekhar et al., (2016) discuss the use of the production data to understand the variation in the drawpoint performance (average grade, final extraction ratio etc.) for an operating SLC mine.

### Draw control strategies

The issue of when to stop loading from the drawpoint and blast the next ring is at the centre of draw control strategies (Sarin, 1981). Initially, draw control strategies were a combination of grade control and tonnage-based loading control systems, but newer strategies use techniques such as material flow prediction, and simulation (Chitombo, 2010; Shekhar et al., 2016).

### Table II

<table>
<thead>
<tr>
<th>Draw control strategy</th>
<th>Principle</th>
<th>Advantages</th>
<th>Disadvantages</th>
<th>Mines (past and present)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Tonnage-based draw control</td>
<td>Ellipsoidal material flow</td>
<td>Simple application, active drawpoint monitoring</td>
<td>Assumption of uniform draw</td>
<td>Craigmont, Granduc, Shabanie, Frood-Stobie, Big Bell, Kirunavaara</td>
</tr>
<tr>
<td>REBOP model</td>
<td>Discrete element method</td>
<td>Uses mechanical laws, predictive in uncertain situations, initial flow similar to real conditions, mine-specific calibration</td>
<td>Absence of drawpoint monitoring</td>
<td>Northparkes, Palabora, Henderson, Cullinan</td>
</tr>
<tr>
<td>PCSLC model</td>
<td>Template mixing</td>
<td>Complete mine planning tool, quick runtime, provides alternative scenarios, considers proximate drawpoint performance</td>
<td>Absence of drawpoint monitoring</td>
<td></td>
</tr>
<tr>
<td>Dilution entry method</td>
<td>Column mixing and interactive draw</td>
<td>Simple application, provides alternative scenarios</td>
<td>Assumption of uniform material flow and interactive draw</td>
<td>Ridgeway, Telfer</td>
</tr>
<tr>
<td>Draw bin method</td>
<td>Column mixing and interactive draw</td>
<td>Recovery curves used to simulate material flow from above levels</td>
<td>Inadequate for incorporating historical production data, slow processing</td>
<td>Telfer, Ridgeway Deeps</td>
</tr>
<tr>
<td>NSO model</td>
<td>Power geotechnical differential mixing</td>
<td>Considers proximate drawpoint performance, provides optimal draw control strategy</td>
<td>Uses mill grade instead of mine grade for calibration</td>
<td>Telfer</td>
</tr>
<tr>
<td>Non-dilution method</td>
<td>Gravity flow</td>
<td>Decreased dilution, active drawpoint monitoring, flexibility in mine layout</td>
<td>Absence of sustainable mine economics</td>
<td>Jing Tie</td>
</tr>
<tr>
<td>PGCA</td>
<td>Cellular automata</td>
<td>Considers proximate drawpoint performance, NPV optimization</td>
<td>Uses mill grade instead of mine grade for calibration</td>
<td>Ernest Henry</td>
</tr>
</tbody>
</table>

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Castro, Gonzalez, and Arancibia, 2009; Gustafsson, 1998; Sellden and Pierce, 2004). Detailed work on gravity flow and flow simulation techniques, along with software developments, has produced several types of flow-based software and modules for simulating flow behaviour that can forecast drawpoint performance (Cundall, Mukundakrishnan, and Lorig, 2000; Diering, 2007; Pierce, 2010). This work is based on physical modelling, including small- to large-scale models (Castro, Trueman, and Halim, 2007; Halim, 2006; Power, 2004). Research using marker trials has been done in SLC mines in Sweden, China, and Australia (Brunton, 2009; Gustafsson, 1998; Power, 2004; Wimmer et al., 2012, 2015). The marker trials have provided important knowledge on flow behaviour and dilution behaviour and have been used for mine design and ore flow simulation in SLC mines. Table II shows eight draw control strategies used in various mines; in-depth descriptions of these strategies follow.

Tonnage-based draw control strategy

Draw control strategies based on fixed tonnage and grade control at the drawpoint are collectively termed ‘tonnage-based draw control strategies’. A fixed target tonnage is decided for a ring based on drill pattern design and recovery targets. Loading is stopped when the target is achieved or when the ore grade drops below the shut-off grade (Baase, Diment, and Petrina, 1982; Hancock and Mattson, 1982; McMurray, 1982; Sarin, 1981).

Draw control based on REBOP model

REBOP stands for Rapid Emulator Based on PFC (Particle Flow Code) (Cundall, Mukundakrishnan, and Lorig, 2000). REBOP calculates contact forces and the resultant Newtonian dynamics of particles (Cundall, Mukundakrishnan, and Lorig, 2000). REBOP’s main objective is to simulate flow within caving operations based on the concepts of isolated movement zones (IMZs) for a drawpoint (Cundall, Mukundakrishnan, and Lorig, 2000; Pierce, 2010). The model uses a layer concept, whereby an IMZ is comprised of a number of disk-shaped layers, and its growth is based on incremental laws applied at layer levels (Pierce, 2010; Sellden and Pierce, 2004). The three main principles guiding the growth of IMZ are porosity, collapse, and erosion (Pierce, 2010).

Draw control based on PCSLC model

The PCSLC model is a mine planning module developed in Dassault Systemes Geovia’s GEMS software by Villa and Diering (2010). The model uses the template mixing algorithm which defines material movement principles for different phenomena, such as vertical mixing, rilling, fines migration etc. (Diering, 2007). The objective is not to simulate flow based on gravity flow conditions (Cundall, Mukundakrishnan, and Lorig, 2000), but to predict the material grade extracted at the drawpoint for a given set of flow conditions (Villa and Diering, 2010). This prediction is used for grade forecasting and production scheduling. The orebody is first divided into cells. The cells are then used to make a production ring. Each element or cell can be depleted, and when sufficiently depleted, it is replenished (Salinas, 2001). A cell is connected to other cells based on a weighted system used for neighbour calculations. The weight is directly proportional to the distance from the main cell to the sampling point of the neighbouring cell (Salinas, 2001). A 3D cone is used to select the cells to be used for building weights, as shown in Figure 3. Thereafter, the selected cells’ weight contribution is assigned to the closest suitable production ring (Salinas, 2001).

Dilution entry method

Laubscher (1994) gives a formula (Equation [2]) for calculating dilution entry, based on which the extraction ratio and ore grade performance can be calculated for block caving:

\[ \text{Dilution entry} = \frac{(A - B)}{A} \times C \times 100 \]  

where \( A = \text{draw-column height} \times \text{swell factor} \); \( B = \text{height of interaction} \); \( C = \text{draw-control factor} \). The draw control factor is based on the variation in tonnage from working drawpoints. The dilution entry method gives ore composition versus extraction ratio curves for different values of dilution entry, as shown in Figure 4 (Bull and Page, 2000). Dilution is given a grade which is an average of the top cave material grade and the grade of unrecovered material from the above levels (Bull and Page, 2000). Cut-off grades are calculated based on mine economics, and shut-off grades are re-estimated using marginal cost information to analyse the feasibility of the shut-off grade with respect to depreciation and repayment (Bull and Page, 2000). These guidelines, along with the curves, provide the basis for draw control. The curves are used to complement mine planning and scheduling.

Draw bin method

The draw bin method was first implemented in a feasibility
Draw control strategies in sublevel caving mines

The Newcrest dilution model and SLC optimizer

The Newcrest dilution model and SLC optimizer can be divided into a recovery model and a draw strategy optimization model (Jamieson, 2012). The recovery of each cell is calculated based on Equation [3]:

\[ T = ta \left( 1 - e^{-A(1-\theta)} \right) \times \left( 1 - C \left( 1 - e^{B(1-\theta)} \right) \right) \]  

where \( T \) = recovered tons; \( ta \) = tons available for recovery; \( A \) = model calibration factor; \( B \) = model calibration factor; \( C \) = model calibration factor; \( DL \) = recovery level (1st level = 1, 2nd level = 2, etc.) within the column.

Recovery Model: The above equation is based on the principle that tonnage extracted from a cell in SLC should be a function of the tons drawn from the cell and the tons remaining in the cell that are available for extraction (Jamieson, 2012). The recovery model is calibrated by correlating the diluted average grade from past production data with the actual average grade measured at the mill. In the study described above, the gold equivalent was used for calibration at Telfer and Ridgeway Deeps, as both mines produce copper and gold.

Draw strategy optimization model: In this model, the values of model coefficients \( A, B, \) and \( C \) (from Equation [3]) are used to optimize the planned extraction ratio for all remaining production rings. The draw control strategy at Telfer was optimized by maximizing the undiscounted profit of each draw column (Jamieson, 2012). This was achieved by calculating the diluted ore value against the variable mining cost, as shown in Figure 6. It took only 2–3 hours to completely analyse more than 2 million draw strategies over the SLC footprint (Jamieson, 2012).

Non-dilution draw method

Zhigui and Xingguo (2008) state that cut-off grades are the primary problem with the current draw control strategies and propose a ‘non-dilution draw’ method. In this method, loading should be stopped as soon as dilution is encountered at the drawpoint (Zhigui and Xingguo, 2008). More specifically, loading is stopped when the muckpile appears to contain 5–10% waste. Physical modelling results show dilution of 4–6% for the non-dilution draw method and a dilution of 15–30% for the cut-off grade method (a waste-to-ore ratio at the muckpile of 30:70), while the ore recovery targets remain the same (Zhigui and Xingguo, 2008). In field tests performed in test areas at Jing Tie iron ore mine, China (Zhigui and Xingguo, 2008), the method was used with a shut-off grade of 10:90 waste-to-ore ratio. Ore recovery was calculated for different sublevels. The ore recovery for the first two sublevels was affected by the switch in the draw control system, but the ore recovery for the third and subsequent sublevels reached the same level as with a traditional draw control system, i.e., approximately 90% (Zhigui and Xingguo, 2008).

Power geotechnical cellular automata (PGCA)

PGCA is a particle-to-particle-based flow modelling technique that can account for material properties, cave back constraints, and other aspects relevant to material flow in caving (Power and Campbell, 2016). The model was calibrated against initial marker trial results at Ernest Henry mine, Australia. In future studies, the model will be calibrated against complete marker trial results, thus providing a better prediction of mineral production and material flow (Power and Campbell, 2016).

Draw control strategy at Malmberget mine

Malmberget mine consists of about 20 orebodies, 12 of which are currently being mined. The mining area stretches 5 km in the E-W direction and 2.5 km in the N-S direction (Lund, 2013). The ore is composed of magnetite (95%) and haematite (5%), and the grade for the different orebodies varies from 49% to 63% (Lund, 2013).
Draw control strategies in sublevel caving mines

Loading criteria

Loading criteria are a set of rules and guidelines that control the loading and closing of drawpoints in SLC. Malmberget has three loading criteria. The operators should continue loading until:

- The extraction ratio (ratio of the total tonnage of material loaded from the drawpoint to the estimated tonnage of the ring) percentage is > 80%
- The average Fe content for the last 25% units of the material loaded (extraction-ratio wise) falls below 30%, and
- The trend of the Fe% for the last 40% units of the material loaded (extraction ratio wise) is negative.

At this point, only the two first criteria are displayed in the LHD cabin for operators. In general, loading is stopped when all three loading criteria are fulfilled, but closing the drawpoints is a subjective decision and depends on the assessment of the loading personnel. If a drawpoint performs poorly from the start (low ore grade), the drawpoint can be closed before an 80% extraction ratio is reached.

Loading near ore boundaries: The loading process and draw control near the hangingwall are guided by safety concerns. Drawpoints near the hangingwalls have an open cavern at later stages of the draw; the hangingwalls have not yet caved or have just started to cave, creating an open cavern above the drawpoint. Keeping this in mind, LHD operators load the material under the supported part of the drift. The current loading procedure dictates that no loading can be performed in an open cavern situation. As a result, loading is stopped when an opening is encountered at the drawpoint because of a gap between the muckpile and the drift roof. The last rings drilled in the production drifts have their drill collars in the footwall, resulting in internal dilution. Because of this, the loading procedure for these rings is different. The initial inflow of waste is neglected, and extraction ratio targets for these rings are kept higher to recover ore that may have accumulated in the upper part of the rings. For the last drawpoint of a production drift, the second phase of loading, known as 'restmalmsslasting' (residual ore loading), is sometimes done by reopening the closed drawpoint to recover known ore remnants. This is a separate process; it assumes that heavy finer ore flows faster than caved material (Kvapil, 1982) and accumulates at the bottom of the caved material.

Loading constraints

For an SLC operation, loading constraints include production requirements, grade control, and mining constraints. Production requirements deal with planned yearly, monthly, and weekly production targets which must be reached for mine operation to be profitable. Grade control is guided by the overall average run-of-mine grade and the shut-off grade for material loading at the drawpoint. Mining constraints are a set of defined constraints to ensure a safe and efficient operation. They include aspects of the mining sequence, continuous cave propagation, blasting, seismicity, available orepasses etc.

Malmberget mine produced around 16.4 Mt of crude ore in 2016; most of this (14.5 Mt) was magnetite, and the rest was haematite (LKAB, 2016). The mine production and scheduling are controlled by short-term plans (three months) discussed in monthly meetings of production and mine planning personnel. Based on the production details of the past month, the short-term plan is revised to meet future production targets. The planned tonnage of a production ring at Malmberget mine varies, as the mine design varies for different orebodies and ore geometries. Hence, the total production target is divided into approximate targets for individual orebodies using the planned tonnage data from the drill pattern design and resource availability. The average mine and shut-off grades for Malmberget vary and are decided by the mine. In 2015, the average mine grade target was 42% Fe, and the shut-off grade target was 30% (Shekhar, Gustafson, and Schunnesson, 2017). However, the average final extraction ratio was 120%, with an average grade of 40.6% Fe. The mine has now increased the shut-off grade from 30% Fe to 35% Fe to reach the overall average grade of 42%. The mine uses a constant grade of 45% for production planning in both short- and long-term plans to simplify the planning process.

Mining constraints for draw control

The mining sequence at Malmberget varies for different orebodies and mining levels but is primarily guided by the development situation of the level below. If there is no development at the lower level or in case of complete development at the lower level, a production ring can be blasted and loaded. However, if partial development is under way, mining cannot be done in those production drifts. In general, a straight cave front is maintained; this transitions into a V-shaped sequence with the vertex pointing away from the entry point into the orebody. The mining sequences vary for different orebodies and are decided by the mine. A minimum distance of 30 m is maintained between production faces for two levels, such that the lower production level lags the upper production level by a distance of 30 m.

The mining sequence is also changed if mining-induced seismicity occurs. Mining-induced seismic events can be harmful to the infrastructure and buildings in the city near the mine; in some cases, they can pose a danger to the miners and the mine’s infrastructure. If the loading at drawpoints in active seismic areas is not safe, the mining sequence is changed by halting the loading process in these areas or postponing blasting until the situation is stabilized.

To achieve its production target, Malmberget mine needs 11 active production areas in the eastern fields and two to five in the western field. Each production area needs two to three active drawpoints. Hence, the mine currently has between 18 and 25 active drawpoints.

At different times, some orebodies in Malmberget may lack the required number of active orepasses because of blockage, damage, or the lack of other orepasses close to the orebody. In these situations, LHDs can load directly onto trucks but this reduces productivity. Blasting constraints at Malmberget are designed to reduce the number of seismic events and decrease the vibrations caused by blasting. Each night, on average, five to six blasts are conducted.

Loading issues

Loading at the drawpoint is sometimes stopped early because...
of loading issues, such as hang-ups, brow failure, pillar failure, intrusions, poor fragmentation, or ring freezing. If a hang-up is encountered in the Malmberget mine, it is first handled by the operator using the LHD bucket to resume the material flow. If the operator is unable to release the hang-up, it is inspected and classified as a high hang-up or a low hang-up. The material can be loaded from the adjacent drift to try to loosen and release low hang-ups, while a production ring can be blasted in the adjacent drift to release high hang-ups. Low hang-ups caused by big boulders are sometimes drilled and blasted by a special crew.

Some orebodies in Malmberget contain waste intrusions. Production rings with a very high percentage of waste intrusion (visualized in the WOLIS system) are not drilled but left intact. A new opening is made, the next set of production rings is drilled and blasted, and loading is continued. The mine is contemplating changing this practice, as it causes problems with reinforcement and caving. In the future, the mine plans to drill and blast the ring but not load the material. Orepasses in Malmberget mine have no grizzly ore boulder breakers. Instead, boulders are transported to a separate drift where they are broken by drilling and blasting.

**Draw control strategy at Kiirunavaara mine**

Kiirunavaara mine is the largest underground iron ore operation in the world. SLC is the mining method practised. The Kiirunavaara orebody consists of magnetite ore with magmatic intrusions. The orebody stretches about 4 km along strike in the N 10° E direction with an average width of around 80 m, dipping at about 60° SE towards Kiruna city (Nordqvist and Wimmer, 2014). The average iron content of the orebody is 64% (Nordqvist and Wimmer, 2014).

**Loading criteria**

The loading criterion in the Kiirunavaara mine is the extraction ratio, which is the ratio of the total tonnage of material loaded from the drawpoint to the estimated tonnage of the ring. Loading from a blasted ring is continued until the extraction ratio has reached the target value communicated by loading control.

**Loading constraints**

The Kiirunavaara mine produced around 26.9 Mt of crude ore in 2016 (LKAB, 2016). Average tonnage of a production ring at the mine can vary from 7 000 to 12 000 t, which translates into more than 3000 rings per year. The average mine grade and shut-off grade vary and are decided by the mine. The current average mine grade is 45% Fe, and the current shut-off grade is 30% Fe (Shekhar, Gustafson, and Schunnesson, 2017). This means that the target average grade for crude ore mined throughout the year is 45% Fe, and a drawpoint should be closed when the grade of the material being loaded falls below 30% Fe. The average mine grade and shut-off grade can change depending on mine conditions and are revised periodically by mine management. The ability to change grade values is essential in order to maintain a profitable operation, but the changed value should be reflected in the draw control strategy by adjusting either the extraction ratio targets or production capacity.

**Mining constraints for draw control**

In the initial mining sequence in a mining level at Kiirunavaara, the cave front is kept flat; i.e. it is moved uniformly along the adjacent drifts of the mining level. As production advances, the flat front pattern changes to a V-shape. The V-shaped cave front can change depending on the mining sequence in nearby mining blocks, seismicity, and structural stability of the drifts in the block. In general, the cave front converges towards the entrance crosscut. Figure 7 shows an example of a mining sequence at Kiirunavaara. Mining blocks with structurally unstable areas are prioritized for blasting and loading.

Twelve active production areas are required to achieve the production target. Each production area needs two to three active drawpoints. Like Malmberget, Kiirunavaara is constrained by seismicity and blasting. The mine also has constraints based on vibration levels in the nearby city of Kiruna, but this is not a source of concern for the mine itself. Mine seismicity is a constraint during operations and affects the sequence of mining in the mining blocks; the number of blasts in a particular mining block is limited to reduce seismicity. Orepass availability is another mine constraint that affects production scheduling. Issues related to seismicity and orepass availability are taken into account during planning and scheduling.

**Loading issues**

When a hang-up is encountered at the drawpoint, it is initially handled by the LHD operator. The operator directs the LHD bucket to penetrate the muckpile and release the hang-up by disturbing the muckpile at the draw point. The ability of the operator to handle the hang-up depends on the nature of the hang-up and the operator’s experience. A high hang-up is difficult to manipulate using the LHD bucket, but a low hang-up can often be resolved. If the operator is unable to handle the hang-up, water jets are used to release it. In other cases, low hang-ups caused by boulders are drilled and blasted. If the hang-up persists, the next production ring is drilled and blasted. In Kiirunavaara mine, boulders are broken by a stationary, remotely controlled rock breaker located at the grizzly at the top of the orepass. Nonetheless, boulders are frequently encountered in the orepass and in the lower output of the orepass. This causes problems when material is being loaded onto the train from the orepasses and is handled either by blasting or by using water jets.
# Draw control strategies in sublevel caving mines

## Table III

<table>
<thead>
<tr>
<th>Draw control strategy</th>
<th>Grade control</th>
<th>Production demands</th>
<th>Extraction ratios</th>
<th>Grade monitoring</th>
<th>Proximate drawpoints</th>
<th>Flow nature</th>
<th>Dilution behaviour</th>
<th>Mine design and layout</th>
<th>Orebody geometry</th>
<th>Mine experience</th>
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<tr>
<td>Kirunavuara and Malmberget</td>
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</table>

## Table IV

### Qualitative assessment of draw control strategies used in sublevel caving mines

<table>
<thead>
<tr>
<th>Draw control strategy</th>
<th>Score</th>
<th>Limitation</th>
<th>Qualitative assessment</th>
</tr>
</thead>
<tbody>
<tr>
<td>Traditional strategy</td>
<td>5/10</td>
<td>These sets of models assume uniform flow, which is incorrect based on current knowledge.</td>
<td>Not recommended.</td>
</tr>
<tr>
<td>(Cokayne, 1982)</td>
<td></td>
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</tr>
<tr>
<td>REBOP mode</td>
<td>7/10</td>
<td>The model does not incorporate grade monitoring and other mining constraints.</td>
<td>Needs improvement before being used for draw control purposes. The model is built mainly to predict the cavefront in caving operations.</td>
</tr>
<tr>
<td>(Cundall, Mukundakrishnan, and Lorig, 2000)</td>
<td></td>
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</tr>
<tr>
<td>PCSLC model</td>
<td>6/10</td>
<td>The model does not incorporate grade monitoring and assumes that ore left on the above level will be recovered on below levels, but the literature shows a more chaotic flow.</td>
<td>Needs improvement before being used for draw control purposes, e.g. incorporate grade monitoring, revise recovery models.</td>
</tr>
<tr>
<td>(Diering, 2007)</td>
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<tr>
<td>Dilution entry</td>
<td>5/10</td>
<td>The model assumes uniform flow, which is incorrect based on current knowledge. Interactive draw is also assumed, which is not always applicable.</td>
<td>Not recommended.</td>
</tr>
<tr>
<td>(Bull and Page, 2000)</td>
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</tr>
<tr>
<td>Dilution bin method</td>
<td>6/10</td>
<td>The model does not incorporate grade monitoring and has slow processing time.</td>
<td>Has been used for draw control purposes but would benefit significantly by improving the model, e.g. assuming chaotic flow.</td>
</tr>
<tr>
<td>(Jamieson, 2012)</td>
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<tr>
<td>NSO model</td>
<td>7/10</td>
<td>The model lacks the input from a robust drawpoint monitoring system. It is difficult to assess copper or gold grade properly in a multiple through visual and assaying techniques.</td>
<td>Is being used for draw control purposes but would benefit significantly from improvement in drawpoint monitoring technology, e.g. XRF or spectroscopic techniques.</td>
</tr>
<tr>
<td>(Jamieson, 2012)</td>
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<tr>
<td>Non-dilution method</td>
<td>5/10</td>
<td>The method ignores the sustainable mining and resource conservation principles.</td>
<td>Not recommended.</td>
</tr>
<tr>
<td>(Zhigui and Xingguo, 2008)</td>
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<tr>
<td>PGCA model</td>
<td>8/10</td>
<td>The model lacks the input from a robust drawpoint monitoring system as it is difficult to assess copper or gold grade properly using current methods.</td>
<td>Is being used for draw control purposes but would benefit significantly from improvement in drawpoint monitoring technology, e.g. XRF or spectroscopic techniques.</td>
</tr>
<tr>
<td>(Power and Campbell, 2016)</td>
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</tr>
<tr>
<td>Kirunavuara and Malmberget</td>
<td>7/10</td>
<td>The model is guided by short-term monitoring of active drawpoints and lacks a holistic approach to draw control.</td>
<td>Is being used for draw control purposes but would benefit significantly by implementing a more dynamic approach.</td>
</tr>
</tbody>
</table>
Draw control strategies in sublevel caving mines

Qualitative comparison of current draw control strategies

Detailed descriptions of draw control strategies used at sublevel caving mines around the world are normally not published in the open literature, making it difficult for researchers to objectively assess their quality. Table III summarizes important factors for a draw control strategy and notes whether these are considered in the available draw control strategies. The table does not, however, comment on the validity of the factors. For example, the dilution entry method uses dilution behaviour (Bull and Page, 2000), but recent marker trials (Brunton, 2009; Nordqvist and Wimmer, 2014; Power, 2004; Wimmer et al., 2015) show that the inclusion of dilution behaviour may be incorrect. Similar arguments can be put forward about other methods.

Recent marker trials highlight the chaotic and random nature of material flow in SLC. Draw control strategies based on ore flow prediction models suffer from a lack of complete knowledge of the chaotic and random nature of material flow. Other control strategies lack a robust drawpoint monitoring system and rely on mill grade to assess drawpoint performance. The draw control strategies in Table III are assessed based on 10 factors. From a critical review of the published literature, a qualitative assessment can be performed to score the different draw control strategies and comment on their use in mines (Table IV). The score of each draw control strategy is augmented by 1 for each of the 10 factors mentioned in Table III that it takes into consideration e.g. a score of 7 out of 10 is given to the REBOP model as it considers 7 out of the 10 factors based on the review of published literature (Table IV). The assessment shows that traditional draw control strategy, dilution entry method, and non-dilution methods are not recommended for draw control purposes. Other methods discussed would benefit significantly from either model improvement or improvement in drawpoint monitoring technologies.

Guidelines for designing a new draw control strategy

An optimized draw control strategy is vital to ensure improved ore recovery and reduced dilution. It will give SLC mines the flexibility to change their production strategy to better align with production demands and metal prices. Based on the literature review and our baseline mapping study, we propose the following guidelines for a new draw control strategy:

1. Effective drawpoint monitoring system: The non-uniform nature of material flow in SLC requires constant monitoring to control dilution while also recovering more ore at the drawpoint. New drawpoint monitoring technologies (e.g. XRF, spectroscopy) must be developed to monitor material grade for copper, gold, or other metals being mined using caving operations. However, for certain cases such as diamond mining, sampling is difficult and 'observational' assessments of dilution are required or other indicators for drawpoint monitoring should be created. A drawpoint monitoring system that can provide a reasonably accurate estimate of the material grade loaded is a requirement for an efficient draw control strategy.

2. Clearly defined production demands and grade controls: The mine should have well-defined production demands in terms of production targets, active drawpoint requirements, and sequencing schemes. Cut-off and shut-off grades should be included in the draw control strategy as well, while grade control should include marginal operating costs and metal prices to provide a more accurately calculation of the average ore grade and shut-off grade.

3. Sensitivity to mine design and ore geometry: A dynamic draw control strategy should be sensitive to the mine design and ore geometry. Basic ore flow principles, results from experiments, and past production data can provide a framework to optimize draw control to a mine’s specific design and ore geometry requirements.

4. Careful assessment of dilution behaviour: The results of past studies show that dilution entry in its present form cannot be a useful parameter for judging drawpoint performance because of the non-uniform or chaotic nature of ore flow. A new method based on production data or other mine-specific parameters for dilution entry and total dilution calculation needs to be developed. An assessment of dilution behaviour is required to decide which aspects of dilution, if any, should be included in the draw control.

5. Influence of proximate drawpoint: Marker trials have shown that secondary and tertiary recovery play an important role in ore recovery. The performance of the drawpoints above a blasted ring has an influence on the ore grade that will be extracted from the drawpoint. However, certain assumptions, for example assuming low extraction ratios in upper levels will translate into more ore at levels below, should not be made because of the chaotic nature of the material flow. A suitable parameter to measure the ore potential from above levels needs to be defined and used in correlational analysis to understand the effects of secondary and tertiary recovery on drawpoint performance. An efficient draw control strategy needs to include such correlations between proximate drifts to better predict drawpoint performance.

6. Mine experience: The performance of drawpoints and the nature of material flow can vary from mine to mine. Historical data on drawpoint abandonment and other performance-related information can provide a baseline for assessing loading issues. A draw control strategy can use this information to reduce dilution and improve ore recovery. Mine personnel observe different aspects of loading during their working hours. They are also familiar with production-related problems. Conducting a qualitative analysis of the information collected from personnel can yield useful results.

Concluding remarks

The current draw control strategies at Kiirunavaara and Malmberget mines are guided by a bucket-weight-based drawpoint monitoring system. The loading criteria at Kiirunavaara mine use the extraction ratio as the primary input information for draw control. Malmberget mine has slightly more flexible loading criteria and uses both extraction ratio and trend of bucket grade as the primary input information. However, both employ a draw control...
strategy that considers the production requirements and mining constraints and regulates the loading process through a continuous drawpoint monitoring system. This continuous drawpoint monitoring system has yielded a large amount of production data in the past 10 years.

The mines currently use an empirical method based on bucket weights and grades. However, there is an opportunity to create a new draw control strategy using the above guidelines. The model must be probabilistic to capture the uncertainties and variations in the caving process and must depend on a robust bucket-weight-based drawpoint monitoring system. Such a model could aim at grade forecasting at the drawpoint and assist in evaluating the economic performance of the drawpoint. The aim of a draw control strategy should be to provide information on when to close an active drawpoint, not to predict material flow. Therefore, draw control is an operational issue and, as such, it should be guided by production data, mine economics, and safety.

Acknowledgement

The authors acknowledge LKAB for its financial and infrastructural support. The authors are also grateful for valuable inputs and support from the staff and management of the Malmbeter and Kiirunavaara mines. Agio System och Kompetens AB, Boliden Mineral AB, ABB AB, and RTC are also acknowledged for their valuable input to the project. Finally, Vinnova, the Swedish Energy Agency and Formas are acknowledged for partly financing this project through the SIP-STRIM programme.

References


References


References


References


References


References


Draw control strategies in sublevel caving mines


OBJECTIVES
The conference will focus on improving health, safety and the environmental impact in the mining and metallurgy industry and highlight actions to be taken. It will act as a platform for learning and allow people to share ideas on safety, health and the environment as well as local communities relationship (issues).

This conference aims to bring together management, DMR, Chamber of Mines, Unions and health and safety practitioners at all levels from the industry to share best practice and successful strategies for zero harm and a value–based approach to health and safety. It will address the main challenges in the mining industry such as logistics, energy and safety of employees, contractors and the communities.

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Investigation and modelling of work roll temperature in induction heating by finite element method

by L. Bao*, X-W. Qi*, R-B. Mei†*, X. Zhang†*, and G-L. Li‡

Synopsis

An induction coil was designed successfully to heat a work roll online for hot or warm strip rolling. The surface temperature of the work roll was investigated through embedding a developed program into finite element (FE) software. A new model to describe the convective heat transfer coefficient between air and the work roll was proposed and the coefficient was obtained as a function of ambient and surface temperature. The influences of working frequency, source current density, air gap length, and coil distance on the mean and variance value of the surface temperature were investigated using the simulation results. The work roll surface temperature was significantly higher than that of the centre due to the skin effect. A longer induction heating time was beneficial for a more uniform temperature distribution on the work roll surface. The mean temperature increased exponentially with increased working frequency and coil distance. However, the mean temperature decreased with increased air gap between the coil and work roll surface. The mean value increased following a power function with increases in source current density, and increased linearly with induction heating time after the initial heating stage. A new formula was proposed and implemented to predict the mean work roll temperature based on induction heating parameters in order to control the surface temperature. The results predicted by the model agree well with measured values and the model proposed is reliable. Furthermore, the proposed equation is beneficial for calculating and controlling the value of any one influencing parameter, if the other three parameters and the ideal mean temperature of induced heating are known.

Keywords
finite element method, induction heating, temperature, work roll, strip rolling.

Introduction

Hot and warm rolling technology has been used widely to produce various metal strip products since 1880, when Japan first began steel production (Ataka, 2014). In hot rolling, the workpiece is usually heated in a furnace and the temperature decreases significantly during the subsequent slab or strip rolling process. The decreased surface temperature may cause non-uniform deformation and crack generation, especially in metals with poor plastic deformability, such as magnesium and high-silicon steel. Fischer and Yi (1964) first proposed a rolling method with heating of the work roll to 538°C to avoid the rapid decrease in strip temperature. The advantage of rolling mills with heated equipment compared to conventional rolling mills was also discussed in detail. Cr$_3$C$_2$ was used as the roll sleeve and steel as the main body of the work roll (Fischer and Yi, 1964). After that, methods and equipment for rolling with a heated work roll were proposed and designed by different researchers (Kang, Wang, and Lv 2005; Zhang, Wang, and Wang, 2008; Li et al., 2012) in order to avoid surface oxidation and improve the plastic deformation limit of hard-to-form materials. The preheated work roll is significantly helpful for large plastic deformation and grain refinement. Jeong and Ha (2007) discussed the influence of the work roll temperature, initial thickness, thickness reduction, rolling temperature, and rolling velocity on the microstructure and texture of AZ31 magnesium alloy during warm rolling. No cracks were found in the strip with a reduction of 30%, rolling velocity of 50 m/min, rolling temperature of 200°C, and work roll temperature of 110°C. Furthermore, the grain size was refined significantly with a single-pass 50% reduction after preheating of the work roll. Kim et al. (2014) used a preheated work roll to realize continuous high-ratio differential speed rolling, maintaining the strip temperature within an ideal range for large plastic deformation and dynamic recrystallization. This method refined the grain size of the workpiece from 11.1 μm to 2.3 μm.

In the previous work, the resistance heating method was mainly used to heat the work roll. The lower heating efficiency of the resistance heating method causes difficulties in application to online rolling. Induction heating is widely used in hot or warm rolling processes (Lainati, 2015) because it offers high efficiency and heating rates (Davies, 1990). In order to assess the possibilities of obtaining the required parameters of induction heating...
Investigation and modelling of work roll temperature in induction heating by finite element

heating, Barglik (2011) carried out three-dimensional analysis of electromagnetic and temperature fields in a transverse flux induction heater for thin strips using the finite element method (FEM) and compared the calculated results with the measured data. Ross (2003) investigated different solenoidal and transverse flux heating methods for strip heating and discussed their application to galvanizing and strip preheating based on experimental data. The higher heating efficiency and no need to make direct contact with the work roll are beneficial to rapid online heating and rolling processes. In order to optimize embossing process, a chiller-equipped heating roll system was developed by Kim et al. (2014). The work roll surface was heated by induction and the temperature was controlled using a temperature-margin-based control algorithm and the measured temperature. New heating equipment for the work roll using induction heating was designed and used in hot and warm rolling by Mei et al. (2016). Complete dynamic recrystallization occurred and ultrafine grains were obtained successfully after three passes of rolling with a cold strip and the heated work roll. However, many factors have an important influence on the temperature of the work roll, leading to difficulty in controlling temperature in induction heating.

Numerical simulations are widely used to solve nonlinear problems, including thermal analysis to improve the accuracy of the predicted and controlled temperature. Local temperature variations induced in both longitudinal and transverse flux induction heaters were studied by Blinov et al. (2011). The temperature of a strip of AZ31 magnesium alloy during strip rolling with a heated work roll was analysed based on the thermal-structural numerical simulation (Mei et al., 2016). The temperature of the cold strip was increased to approximately 200°C after one rolling pass under the conditions of a work roll temperature of 300°C, initial thickness of 2 mm, 20% reduction, and rolling speed of 0.1 m/s. Yu et al. (2012) discussed the changes in strip temperature during AZ31 alloy rolling with a heated work roll and cold strip, and proposed a new model to predict the exit temperature of the strip. Induction heating at different frequencies was investigated by FEM and the calculated results agreed well with the experimental measurements. The transient temperature field of a heated strip or slab was simulated based on the coupled electromagnetic-thermal FEM (Mei et al., 2008, 2010). FEM simulations and experiments were performed to analyse edge defect generation in plate rolling; and it was proposed that increasing the temperature of the edge zone of the slab by induction heating could reduce the possibility of corner crack formation (Pesin and Pustovoytov, 2015). Cai et al. (2013) introduced the idea of warm rolling with a work roll heated by the induction heating method and analysed the temperature distribution of the work roll using a 2D FE model and the heat generation model proposed by Mei et al. (2010).

In the current work, the temperature distribution of the work roll was investigated during induction heating through an FEM 2D coupled electromagnetic-thermal analysis, giving detailed boundary conditions, FE mesh, and a flow chart of the program. The effects of various induction heating parameters, including working frequency, air gap between the coil and work roll surface, source current density, and distance between two coils, on the temperature were studied after changing the mean and variance of temperature based on the simulation results. Subsequently, a new model was proposed to predict the temperature of the work roll surface according to linear fitting and nonlinear induction, and the data predicted by the model was compared with the results calculated by FEM. Lastly, the model was verified by comparing the experimental data and simulation results. This study may be helpful in improving modelling methodology to simulate the work roll temperature in induction heating and accurately control it during roll processing.

FE Modelling

Initial and boundary conditions

The parameters of the work roll were the same as those of the experimental rolling mill used to analyse induction heating (Figure 1). The induction coil consisted of a copper pipe forming a single circle on the work roll surface, with a cross-sectional area of 20 mm². A magnetizer was installed at the outside of the coil (Figure 1a) to improve the magnetic field and heating efficiency. The material of the work roll was hot die steel (CG-2), whose properties are assumed constant (free-space permeability 4π×10⁻⁷ H/m (Mei et al., 2008), expansion coefficient 13.5×10⁻⁶ °C⁻¹ (Xu and Chen, 2001), and density 7900 kg/m³ (Xu and Chen, 2001)) for the purpose of analysing the electromagnetic-thermal field. The relative permeability values of the air and the coil were set to unity (Mei et al., 2008). The effects of temperature on the main electromagnetic-thermal properties (Xu and Chen, 2001) are given in Table I. In order to ensure an adequate service life and reduce the elastic deformation, the work roll is usually not heated to high temperature (Fischer, 1964). Therefore, these properties are given for temperatures less than 700°C, and those at temperatures of 740 to 1200°C can be obtained by linear extrapolation. The relative permeability of the steel changes to approximately unity by thermal demagnetization when the temperature reaches the Curie temperature of about 740°C (Xu and Chen, 2001). After this, the relative permeability decreases slowly to reach 1.0 at 1200°C (Xu and Chen, 2001; Mei et al., 2008). The initial temperature is set to 20°C. The induction heating parameters chosen to analyse the effects on temperature were: working frequency 5–100 kHz; current density 5.0×10⁶ to 1.5×10⁷ A/m²; air gap between coil and work roll surface 5–30 mm; and coil conductor spacing 30–180 mm. The velocity of the work roll was set to 0.05 m/s and the consumed time was set to one rotation of the work roll.

Figure 1—Geometry and FE mesh: (a) geometry; (b) FE mesh
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Table I

<table>
<thead>
<tr>
<th>Electromagnetic-thermal properties of work roll</th>
</tr>
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<tbody>
<tr>
<td>Temperature (°C)</td>
</tr>
<tr>
<td>Conductivity (W/m·K)</td>
</tr>
<tr>
<td>Heat Capacity (J/kg·K)</td>
</tr>
<tr>
<td>Resistivity (Ω·m)</td>
</tr>
<tr>
<td>Temperature (°C)</td>
</tr>
<tr>
<td>Conductivity (W/m·K)</td>
</tr>
</tbody>
</table>

Only the change of surface temperature was calculated during induction heating, and the boundary conditions included convective and radiative heat dissipation. Therefore, the heat transfer coefficient is written as

\[
h_c = h_{rad} = h_{conv}
\]

where \( h_c \) (W/m²K) is the total heat transfer coefficient, while \( h_{rad} \) (W/m²K) and \( h_{conv} \) (W/m²K) are the heat transfer coefficients in the radiative and convective processes, respectively. The surface temperature of the work roll and the ambient temperature affect the heat transfer coefficient in radiative processes; the radiative heat transfer coefficient can be described as

\[
h_{rad} = \frac{\sigma T^4_{wr} + T^4_{air}}{T_{wr} + T_{air}}
\]

where \( T_{wr} \) (K) is the surface temperature of the work roll, \( T_{air} \) (K) is the air temperature, and \( \sigma \) is the Stefan-Boltzmann constant, 5.67x10⁻⁸ W/m²K⁴. The surface emissivity (Zhou, 2003; Han, Lee, and Kim, 2002) \( \varepsilon \) is described as

\[
\varepsilon = 0.125T_{wr}^2/1000 - 0.38
\]

At forced convection is ignored because of the low speed of the work roll (Panjkovic, 2007), the heat transfer coefficient of natural convection can be written as (Dai, 2005)

\[
h_{conv} = 0.48\frac{\rho Pr\alpha^3}{\nu^2}(T_{wr} - T_{air})^{1/2}
\]

where \( g \) (m/s²) is the gravitational acceleration, \( d \) (m) the characteristic size (in this case equal to the work roll diameter), \( Pr \) the Prandtl number, \( \alpha \) (K⁻¹) the coefficient of expansion, \( \nu \) (m/s) the kinematic viscosity, and \( \lambda \) (W/m K) the conductivity of air at a characteristic temperature, and in this case set to about 350°C (Dai, 2005). Referring to the equation given by Panjkovic (2007), and for an assumed mean value of characteristic temperature of air around the work roll surface, the convective heat transfer coefficient can be described as

\[
h_{conv} = 1.49(T_{wr} - T_{air})^{1/2}
\]

FE mesh

One cycle of the square induction coil is designed to heat the work roll. The experimental equipment was designed and the experiments were carried out in a similar fashion. The length of the coil in the axial direction of the work roll was larger than the radius, so the influence of the coil along the circular direction on the induction heating can be neglected. Accordingly, a 3D FE model can be simplified to a 2D problem to reduce the computational time. The distance between adjacent coil conductors in the axial direction is referred to here as the coil distance. The induction current is generated when the source current passes through the coil, which then heats the work roll. In Figure 1b, the red elements express one circular movement of the coil, whereas the fork and point are used to describe the source current direction at a given time during induction heating. The quadrilateral elements in the mapped mesh model provide higher solution precision than triangular elements in a free mesh model. Therefore, the outside of the work roll is modelled using a mapping mesh with quadrilateral elements, while the centre is modelled as a free mesh with quadrilateral elements. Furthermore, the skin depth should have been divided into three or four layers of elements to improve the predicted precision. However, in order to reduce the computational time, these elements were meshed more coarsely from the centre to the boundary (Figure 1b). There were a total of 10 122 elements and 30 427 nodes in the FE mesh, with the numbers of elements and nodes for the work roll being 9782 and 28 807 respectively.

The skin depth is greatly affected by the working frequency, resistivity, and permeability (Davies, 1990; Blinov et al., 2011), which can be expressed as

\[
\delta = \frac{\rho}{\sqrt{\pi \mu_0 \mu_f}}
\]

where \( \delta \) is skin depth, \( \mu \) is relative permeability, \( \mu_0 \) is free-space permeability, and \( f \) the working frequency.

An electromagnetic-thermal coupling model

The interaction of the electromagnetic field with the thermal characteristics impedes the solving of the coupled electromagnetic-thermal field during induction heating. The temperature distribution has a significant effect on the magnetic properties of the material, which then generates different electromagnetic fields. Meanwhile, different induction currents give rise to different Joule heating effects. In addition, the relative positions of the induction coil and work roll surface change with the rotation of the work roll.

The electromagnetic-thermal model was developed in the ANSYS software environment, which employs an iterative solver to solve for the temperature and electromagnetic field. In order to solve problems for the rotation of the work roll by FE, the induction coil was assumed to have the same velocity as the work roll, but in the opposite direction, and
the work roll was assumed to be stationary. During induction heating, the movement of the source current causes the heat transfer boundary to change. Therefore, the transient temperature should be solved with different positions of the source current and boundary conditions through embedding the appropriate programs in the software.

The flow chart of the coupling methodology is shown in Figure 2. Firstly, the geometry and FE mesh were created according to the input parameters, including materials properties, element type, and heating parameters. A series of arrays for storage was defined and the number of elements and nodes on the surface of the work roll were reordered in order to simulate the rotation of the work roll, change of the boundary conditions, and loads. Secondly, the induction heating parameters were set and saved in the generated electromagnetic physics environment. Then the thermal analysis parameters for the elemental model of the work roll in the thermal physics environment were set and saved. In the solution of the electromagnetic field at $t_{i+1}$, the temperature at $t_i$ was used to calculate the electromagnetic properties and the Joule heat was obtained for the solution of thermal analysis at $t_{i+1}$. When the FTIME meets the setting value, the simulation has finished.

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Figure 3 shows the temperature distribution at different times, with an air gap of 10 mm, coil distance of 80 mm, source current density of $7.5 \times 10^6$ A/m$^2$, and working frequency of 50 kHz, where MX and MN are expressed as the maximum and minimum values of the temperature, respectively. The position of maximum temperature changes with the rotation of the work roll. The temperature of the rotating work roll during induction heating was predicted through the developed program-coupled FE model. The maximum temperature occurred in the same region after one rotation, and the skin effect creates a significantly higher temperature at the surface than the centre. The layer distribution of the temperature caused by induction heating is beneficial for maintaining the strip temperature. With increasing induction heating time, the centre is heated by conduction. The temperature at positions closer to the induction coil increased quickly, and the temperature difference was obvious among these surface nodes. The surface temperature changed from 32°C to 88.8°C after one rotation and varied from 327°C to 394°C at 1200 seconds under the above specified conditions.

Both the distribution and homogeneity of the surface temperature have an important influence on the strip rolling process. In order to study the change in temperature with different induction parameters, ignoring edge effects, the mean temperature $T_a$ and variance temperature $S_T^2$ of the nodes in the work roll surface were used to describe the heating efficient and homogeneity (Marck et al., 2012)

$$
T_a = \frac{1}{n} \sum_{i=1}^{n} T_i
$$

$$
S_T^2 = \frac{1}{n} \sum_{i=1}^{n} (T_i - T_a)^2
$$

where $n$ is the number of nodes on the work roll surface. In this study, eight nodes, including the typical nodes of A, B, C, and D (Figure 3f) and four others in the middle (E, F, G, and H), were selected to analyse the mean value and homogeneity.

The change in $T_a$ and $S_T^2$ with induction heating time is shown in Figure 4. It was found that the coefficient of determination was 0.9985 and that $S_T^2$ increased

![Flow chart of the coupling methodology](Image)
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Figure 3—Distribution of temperature with 50 kHz at different times: (a) t = 0.1 s; (b) t = 2.5 s; (c) t = 5 s; (d) t = 7.5 s; (e) t = 10 s; (f) t = 1200 s

Figure 4—Change of $T_a$ and $S_T^2$ with induction heating time

exponentially with heating time. $S_T^2$ increased by 90 seconds from 60 seconds to 300 seconds, but only by 10 seconds within the range of 900 seconds to 1200 seconds. This indicated that $S_T^2$ increases more slowly with increased induction heating time. The $T_a$ value increased significantly with the induction time, with an approximately linear relationship. $T_a$ was approximately 155°C at 300 seconds and 335°C at 1200 seconds. Therefore, a lower temperature at a shorter induction time can be used for rolling with a hot strip. Furthermore, the fitting curves show the coefficients of determination to be 0.9787 if the data point at $T_a$ at 300 seconds is included, and 0.9973 without the data point. The $S_T^2$ value increases more slowly than $T_a$ with increased time, contributing to the homogeneity of the surface temperature.

**Influence of induction heating parameters on temperature**

Due to the lower homogeneity early in heating, the effects of the induction heating parameters on induction heating after 300 seconds were studied. The induction heating parameters, including the working frequency $f_w$ (kHz), air gap $g_a$ (mm) between coil and work roll surface, source current density $c_s$ (MA/m²), coil distance $d_c$ (mm) in the circumferential direction between the two coils, and the heating time have important influences on the changes in $T_a$ and $S_T^2$.

**Working frequency ($f_w$)**

Figure 5 shows the influence of $f_w$ on the $T_a$ and $S_T^2$ values of the surface temperature. A higher working frequency created a more significant skin effect and higher inductive current intensity. $T_a$ increased exponentially with increased working frequency. The minimum of determination coefficients of fit curves was greater than 0.999. Consequently, the fitting model is valid. The mean temperature changed very slowly at 500 Hz. However, as the induction heating time increased from 300 seconds to 1200 seconds, the mean temperature increased significantly from 220°C to 550°C at 100 kHz.
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addition, with increasing working frequency, $S_T^2$ first increased slowly from 500 Hz to 10 kHz, before increasing more quickly from 10 kHz to 100 kHz. The induction heating time has less influence on $S_T^2$ than $f_w$. Despite the higher variance value, the ratio of the maximum temperature difference to the mean value decreased significantly with increased working frequency. Therefore, a higher working frequency is beneficial for the homogeneity of surface temperature.

Air gap ($g_a$)

The effect of $g_a$ on $T_a$ and $S_T^2$ is shown in Figure 6. $T_a$ decreased exponentially with increasing air gap length. The minimum of coefficient of determination was 0.99947, demonstrating that the fitting curves were effective. Increased distance from the coil to work roll surface decreased the intensity of the magnetic induction current and $T_a$ decreased with increasing $g_a$. However, the magnitude of increase was different and a smaller $g_a$ caused a stronger increase in $T_a$. As an example, the magnitude of increase was 100°C with a change of $g_a$ from 10 mm to 20 mm, and 50°C with a change of $g_a$ from 20 mm to 30 mm. The $S_T^2$ value of the surface temperature decreases markedly with increased $g_a$ and decreased induction heating time. $S_T^2$ changes less at 1200 seconds than at 900 seconds with $g_a = 5$ mm because of the higher temperature. Therefore, smaller $g_a$ values and longer induction heating times promote homogeneity.

Source current density ($c_s$)

The influence of $c_s$ on the $T_a$ and $S_T^2$ values of surface temperature is shown in Figure 7. $T_a$ varied as a power function. The minimum coefficient of determination was 0.9993. A higher induction current intensity results from higher $c_s$, so $T_a$ increased significantly with increases of $c_s$. The $S_T^2$ value of the surface temperature increased.
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exponentially with increasing $c_s$. $S_T^2$ increased slowly in the $c_s$ range from 5.0 MA/m² to 10.0 MA/m², but for further increases in $c_s$ to 15 MA/m², $S_T^2$ increased to approximately four times the value at 10.0 MA/m². Thus, the surface temperature became more homogeneous with increased heating time because the increase in $S_T^2$ was less than that of $T_w$.

Coil distance ($d_c$)

The value of $d_c$ can be controlled by changing the number of interval elements (Figure 1b). The influence of $d_c$ on $T_w$ and $S_T^2$ of the surface temperature is shown in Figure 8, with the minimum value of the coefficient of determination being $0.99996$. $T_w$ increased significantly with increases in $d_c$. This was mainly because the reversed direction of the source currents for elements $i$ and $i+1$ (Figure 1) generates an induction current with a reversed direction. The interaction of the reversed induction current decreased the current intensity in the effective regions of the electromagnetic fields. The $S_T^2$ value increased initially for $d_c$ values below 80 mm, but decreased within the range of $d_c$ from 80 mm to 180 mm. Clearly, larger $d_c$ values benefit the homogeneity of the surface temperature. Furthermore, the surface temperature had a nearly constant $S_T^2$ with $d_c=130$ mm.

Modelling of induction heating

In order to predict $T_w$, a new function was proposed according to the curves in Figures 5 to 8. $T_w$ varied exponentially with the air gap, coil distance, and working frequency, while varying according to a power law with increased source current density. Therefore, the function is described as

$$T_w = A_o \cdot (c_s)^{B_0} \exp\left[C_0 (f_w)^{C_1} + D_0 (d_c)^{D_1} + E_0 (d_c)^{E_1}\right]$$

where $A_o$, $B_0$, $C_0$, $D_0$, $E_0$, $C_1$, $D_1$, and $E_1$ are constants.

Taking the natural logarithm of both sides of Equation [8] gives:

$$\ln(T_w) = \ln(A_o) + B_0 \ln(c_s) + C_0 (f_w)^{C_1} + D_0 (d_c)^{D_1} + E_0 (d_c)^{E_1}$$

The values of $C_1$, $D_1$, and $E_1$ are 0.1, –1, and 0.5, respectively, according to the linear fitting precision of the plots of $\ln(T_w)-(f_w)^{C_1}$, $\ln(T_w)-(d_c)^{D_1}$, and $\ln(T_w)-(d_s)^{E_1}$.

Then, the values of $B_0$, $C_0$, $D_0$, and $E_0$ can be obtained as:

$$B_0 = \frac{\partial \ln(T_w)}{\partial \ln(c_s)} \quad C_0 = \frac{\partial \ln(T_w)}{\partial (f_w)_{C_1}} \quad D_0 = \frac{\partial \ln(T_w)}{\partial (d_c)_{D_1}} \quad E_0 = \frac{\partial \ln(T_w)}{\partial (d_c)_{E_1}}$$

The values of the constants $B_0$, $C_0$, $D_0$, and $E_0$ were obtained as $1.748$, $4.854$,$\cdot35.663$, and $-0.231$, respectively, from the mean slope of the lines in the plots of $\ln(T_w)-(f_w)_{0.1}$, $\ln(T_w)-(d_c)^{-1}$, and $\ln(T_w)-(d_s)^{0.5}$ (Figure 9). $T_w$ is therefore described as

$$T_w = A_o (c_s)^{1.748} \exp[4.854(f_w)^{0.1} - 35.663(d_c)^{-1} - 0.231(d_s)^{0.5}]$$

Subsequently, substituting different source current densities, work frequencies, coil distances, and air gaps into Equation [11] with different induction heating times provided the constant $A_o$ as equal to the slope of the fitting curves, as shown in Figure 10. It can be seen that $A_o$ decreased linearly with increased induction heating times. Through the linear fitting, the value is described as:

$$A_o = 0.00735 + 1.557 \times 10^{-5} t$$

Figure 8—Influence of $d_c$ on surface temperature: (a) $T_w$; (b) $S_T^2$

Figure 9—Relationships between $\ln(T_w)$ and (a) $f_w A_1$; (b) $\ln(c_s)$; (c) $1/d_c$; and (d) $d_s^{0.5}$

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According to the above analysis, the model obtained for \( T_a \) in induction heating can be deduced as:

\[
T_a = (735 + 1.557 t) \times 10^{-5} \times (c_2)^{1.740} \exp \left[ 4.854(f_a)^{0.1} - 35.663(d_a)^{-1} - 0.231(g_a)^{0.5} \right]
\]

where \( t \) (s) is the induction heating time.

The temperatures calculated by the new model and ANSYS software were compared to validate the model predictions (Figure 11). The new predicted temperature agreed well with that obtained using the ANSYS software, with the coefficient of determination being 0.99301. Thus, the model proposed and obtained from the FEM data reliably predicted the temperature of the work roll in induction heating. The nonlinear analysis method is suitable for the solution of induction heating and constructing the mathematic model.

Furthermore, the modelling technique and methods are helpful for nonlinear analysis of engineering problems. For the known values of the working frequency, air gap, coil distance, and source current density during induction heating, the mean temperature at any time can be predicted by the model. Furthermore, Equation [13] can be used to calculate the critical value of any one influencing parameter if the other parameters and the ideal mean temperature of induction heating are known. As an example, when the air gap is 10 mm, the source current density is 10 MA/m\(^2\), coil distance is 100 mm, and ideal mean temperature is 300°C, the working frequency should be approximately 40 kHz for heating time of 450 seconds according to the model.

Induction heating experiments have been also carried out to verify the model predictions with the same conditions as in the above example. The relationships between the critical working frequency and idea temperature, mean measured value, and predicted results are shown in Figure 12. In order to reduce error, the position located between typical points D and H was measured rapidly five times in 15 seconds and the mean value recorded. The five measurements for the above experimental conditions were 318°C, 321°C, 319°C, 323°C, and 324°C, and the relative error of these measured values was less than about 2%. Additionally, different experimental

Figure 10—Relationship between (a) \( T_a \) and \( (c_2)^{1.740} \cdot \exp[4.854(f_a)^{0.1} - 35.663(d_a)^{-1} - 0.231(g_a)^{0.5}] \); (b) time and \( A_0 \)

Figure 11—Comparison of predicted temperatures by the model and FEM

Figure 12—Relationship between (a) idea temperature and critical working frequency; (b) measured values and predicted results
parameters were also selected as follows to verify the model: the source current was set to 7.5 MA/m², working frequency was set to 25 kHz, the air gap was 30 mm, and the coil distance was 80 mm, with heating time from 300 seconds to 3600 seconds (Figure 12b). It can be seen that the temperature predicted by the proposed model was in good agreement with the measured value. The maximum error was about 12.4% and the average relative error was 3.9%. Therefore, the model derived from the FEM data is useful for predicting the temperature of the work roll in induction heating.

Conclusions
A program was developed and embedded into the FE ANSYS to simulate the surface temperature of the work roll for metal strip rolling during induction heating. The skin effect created a higher temperature at the surface than that at the centre. The variance value increased exponentially with increased heating time. The mean temperature varied approximately linearly after the initial heating stage of approximately 300 seconds.

With increased working frequency, current density, and coil distance, and decreased air gap, the mean temperature increased exponentially. Furthermore, the variance value increased with increasing source current density and working frequency and decreasing air gap. However, the variance value first increased and then decreased with increasing coil distance, because of the change of the electromagnetic field. Despite the increased variance value of the surface temperature, longer induction heating times promoted temperature homogeneity because the surface temperature changed more dramatically than the variance value.

The relationship between the mean temperature and induction parameters was fitted according to the simulated data. A new formula was developed to predict the mean temperature of the work roll during induction heating, considering the working frequency, air gap, coil distance, source current density, and induction heating time. Furthermore, the formula could be used to calculate the critical value of any one parameter, given the influencing parameter with three known values and the ideal mean temperature of induction heating. The predicted results agreed well with the simulated data by FEM and the measured values from actual induction heating experiments.

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References


WorleyParsons provides innovative solutions for each step of the mining value chain. We combine world-leading, concept-to-completion expertise with design and major project delivery capabilities for the minerals and metals sector, from bulk commodities to rare earths, with complete mine-to-market solutions from inception to rehabilitation. Our Global Centre of Excellence for Mining and Minerals in South Africa has niche expertise in hard rock and precious minerals and metals, and we have achieved particular recognition for the delivery of complex processing plants and deep shaft mines.

**Background**

The number of people involved in artisanal and small-scale mining (ASM) activities have been increasing globally. In Africa, specifically, the figures are estimated to be 9 million. This is an increase from an estimated 3 and 3.5 million in 1999. Presently, the ASM sector accounts for the largest mining workforce in Africa. Since ASM became part of the development agenda in the 1970s, many developing countries have acknowledged the sector’s importance in its role in local socio-economic development and have initiated a series of interventions aimed at formalizing the sector. In parallel, significant amount of research has been conducted to understand the sector and to address the negative externalities associated with the sector which for many decades have hindered the development of the sector in line with the respective national development agendas and on the African continent, the Yaoundé Vision on ASM and the African Mining Vision (AMV). To date, ASM remains a contentious issue characterized by misconceptions brought by the lack of clarity on the definition of ASM and issues surrounding its legitimacy as a source of livelihood.
Interaction between vertical stress distribution within the goaf and surrounding rock mass in longwall panel systems

by P. Wang*, J. Zhao†‡, G. Feng*§, and Z. Wang†

Synopsis

Four main longwall panel systems are used in China: conventional longwall top coal caving (CLTCC), multi-slice longwall mining (MLM), high-seam longwall mining (HSLM), and longwall mining with split-level gateroads (LMSG). Theoretical analyses and physical modelling studies were carried out to investigate the interaction between vertical stress distribution within the goaf and surrounding rock mass in these systems. These studies were supported by numerical analyses and validated by field observation. The difficulty in numerical analysis for longwalls lies in goaf modelling, and research on this is rare. Reasonableness and correctness of numerical modelling is highly dependent on goaf behaviour. A complete and detailed numerical model of stress distribution within the goaf and surrounding rock mass is presented in this paper. A double-yield constitutive model, which is best fitted by Salamon's model that was obtained through laboratory tests, is used to simulate the goaf. The angle of break obtained through physical modelling was also incorporated into numerical modelling, which is closer to practice. The modelling shows that: (1) the more load the goaf bears, the less the abutment pressure and vice versa; (2) the abutment pressure, stress concentration factor, and yield zones would be larger without considering goaf behaviour; (3) goaf pressures in the curved section employing HSLM and LMSG are larger than in MLM and CLTCC; (4) when one slice is being extracted in MLM, the goaf pressure is greater than with any other methods, and the high-stress zone and yield zone are smaller; (5) the goaf edge is the most destressed zone in the entire panel system of the four mining methods. Field observation shows that the intake entry in LMSG has a favorable stress environment. Ground control problems such as severe deformation and bursts in the entry are therefore minimized, which in turn validates the theoretical and modelling analyses.

Keywords
longwall mining, stress distribution, goaf behaviour, numerical modelling, double yield, abutment pressure, surrounding rock mass, split-level.

Introduction

Longwall mining is the main underground coal mining method in China. Since China’s coal reserves occur in a variety of geological environments (thick or ultra-thick coal seams (more than 3.5 m), steeply inclined seams, deep cover, etc.), many longwall panel systems, including multi-slice longwall mining (MLM), high-seam longwall mining (HSLM), conventional longwall top coal caving (CLTCC), and longwall mining with split-level gateroads (LMSG) have been developed (Du and Meng, 2009). Ground pressure resulting from coal extraction is a dominant factor in any failure of a panel. Therefore, determining the stress state of a panel system is crucial for safe production (Wang et al., 2017; Zhao, Hebblewhite, and Galvin, 2000).

Zhenchengdi coal mine is located in Xishan mining area, Shanxi Province, China. A simple mono-synclinal structure is the major tectonic structure. Number 2–4 coal seam is the main seam, with an average dip angle of 4° and cover depth of 230 m. No. 22202 and 22204 panels are 680 m long along the strike and 150 m wide along the dip. The coal seam is 4.5–5.8 m thick, with a low gas content. The roof strata comprise fine-grained sandstone (2.2 m) followed by sandy mudstone (6.4 m), while the floor strata are muddy sandstone (3.1 m) underlain by siltstone (4.2 m). Four longwall panel systems as mentioned above were designed for the mine, and the corresponding panel layouts are shown in Figure 1.

For CLTCC (Figure 1a), the lower section of the seam is cut by the shearer at a set height, and the coal left above the section cut by the machine is induced to cave into the rear conveyor, taking advantage of the fracturing due to the front abutment pressure. MLM (Figure 1b) divides the coal seam into two slices, with each slice longwall-mined sequentially. The upper slice is generally mined first in order to maintain the integrity of the lower slice. HSLM (Figure 1c) involves cutting the whole bed by one web. In LMSG (Figure 1d) gateroads on either end of the panel are located at different elevations in the coal seam, one along the floor and the other along the roof (Zhao, Wang, and Su, 2017; Zhao, 1998). During the last two decades, the mine used HSLM and MLM. However, due to
Interaction between vertical stress distribution within the goaf and surrounding rock mass

The varied height of the coal seam, the HSLM shields could not accommodate the geological conditions and MLM could not achieve high production and high efficiency. Market competition and geological conditions pushed the mine to use CLTCC a few years after HSLM and MLM. Then LMSG was introduced about 8 years ago, resulting in a large increase in profit.

However, there are still many problems with CLTCC. The first is low recovery, because a relatively large gateroad pillar is left unmined between two adjacent panels and the top coal above the 4–5 end shields and the two gateroads is not recoverable using CLTCC. This becomes loose coal, which increases the risk of spontaneous combustion. Furthermore, ground control problems are severe in the gateroad close to the goaf as a result of the side abutment pressure acting on the gateroad pillar. In the past, coal bumps, rib sloughing, and slabbing frequently occurred within the development entry next to the goaf, which ‘parked’ or idled the longwall and caused tremendous production losses. The engineers concluded that the problems were caused by the stress concentration on the gateroad pillar. This remains a difficult problem to solve. On one hand, leaving a large pillar results in low recovery, which is critical owing to the dwindling coal reserve; on the other hand, a small pillar leads to instability and many ground control difficulties. In addition, for the abovementioned four mining methods that the mine has used, the severity of the ground control problems within the development entry next to the goaf vary, which baffled the engineers. It is certain that the behaviour of ground pressure and coal mass are disturbed with decreasing severity from the immediate roof towards the surface (Qian Shi, and Xu, 2010; Peng, 1978). Failure of the roof may continue even after the higher strata and caved material are in contact. The compaction of the caved material increases until a new equilibrium is reached, when no additional strata subsidence occurs and the roof sustains a certain amount of overburden load from the overlying strata (Peng, 1978). For the surrounding rock mass around the goaf, abutment pressure is developed. The next panel is developed a sufficient distance from the over-stressed area and a coal pillar is left unmined between the adjacent panels. Thus the coal pillar is used to sustain the side abutment pressure and maintain the stability of the development gateroad(s) of the active panel.

Wilson (1981) presented an approach for calculating the depth of the plastic zone of a coal pillar. Yavuz (2004) proposed a method for estimation of the cover pressure distance and the pressure distribution in the goaf of flat-lying longwall panels. Morsy and Peng (2002) studied the goaf loading mechanism in longwall coal mines using numerical modelling. Campoli et al., 1993) studied goaf and gateroad reaction to longwall mining in bump-prone strata. Esterhuizen, Mark, and Murphy (2010) calibrated a numerical model for simulation of coal pillars, and the goaf and overburden response. However, the relationship between the goaf behaviour and the surrounding rock mass is still not well understood, especially as regards the different mechanisms behind these different mining methods. In view of the fact that studies of the interaction between goaf behaviour and surrounding rock mass in these longwall mining systems are rare, in this paper we present such a study based on theoretical analysis, physical modelling, numerical modelling, and field observation.

Theoretical analysis

The goaf and surrounding rock mass have a strong interconnectivity. Before the excavation of a longwall panel, pre-mining stress is uniformly distributed. After excavation the stress redistribution results in side abutment pressure and goaf pressure that increases from zero at the goaf edge to the pre-mining stress at a certain distance from the goaf edge, as shown in Figure 2. The stress on the solid coal on the right can be divided into four zones: I – destressed yield zone, II – overstressed plastic zone, III – overstressed elastic zone, and IV – pre-mining vertical stress zone. The limit equilibrium zone \( x_o \) ranges from 3 to 20 m in width and is typically between 5 and 12 m, and can be calculated by (Quan Shi, and Xu, 2010):

\[
x_o = \frac{m}{2 \xi \ln \frac{k \gamma H + C \cot \varphi}{\xi (p_1 + C \cot \varphi)}}
\]  

where \( m \) is the mining height, \( f \) is the friction factor between the coal seam and roof and floor strata, \( \varphi \) is the angle of internal friction of the coal mass, \( C \) is the cohesion, \( \gamma \) is the unit weight, \( H \) is the cover depth, \( k \) is the stress concentration factor, \( p_1 \) is the external support force (steel sets, pipe-shed support, or other type of support) acting on the rib, and \( \xi = \frac{1 + \sin \varphi}{1 - \sin \varphi} \).
The stress redistribution discussed above is an example of the interaction between the goaf and surrounding rock mass. Obviously, this interaction is influenced by many factors such as panel geometry, mining height, and cover depth, because the integral of the vertical stress after excavation plus the stress of extracted coal must equal the integral of the vertical stress before excavation. Thus, the more load the goaf bears, the lower the abutment pressure, and vice versa. Therefore, the load borne by the goaf must have an influence on stress concentration factor \( k \). On the other hand, the load-bearing characteristics of goaf are related to goaf configuration, which is determined by the panel layout or geometry, for different panel layouts lead to different modes of roof strata failure, fracturing, subsidence, and movement, thus influencing goaf configurations. Figure 3 depicts the strata movement and goaf configurations for the different panel configurations in the four longwall mining methods shown in Figure 1.

Esterhuizen, Mark, and Murphy (2010) indicate that fall height, size, and shape of the rock fragments affect the bulking of the goaf, as shown in Figure 4. The greater the fall height, the more likely it is that the fragments will rotate and come to rest in a jumble. For the subsequent falls, fall height is reduced, thus the fragments will be arranged in a more orderly fashion and the amount of bulking will be reduced. Therefore, the greater the mining height, the less the compaction of the goaf, resulting in a smaller goaf pressure. According to the vertical stress integral analysis presented earlier, the pressure deficiency in the goaf will be borne by the abutment pressure.

As a result, for CLTCC (Figure 3a), the symmetrical panel geometry leads to a symmetrical abutment pressure distribution.

For MLM (Figure 3b), when mining the upper slice, the mining height reduces to half of the total seam thickness, which means that \( m \) in Equation [1] becomes \( m/2 \), hence the limit equilibrium zone \( \chi_0 \) is only half of that in CLTCC. What’s more, as the stress concentration factor \( k \) also drops due to the decrease in mining height, \( \chi_0 \) would consequently be less than \( m/2 \). On the other hand, rock fragments are more orderly arranged and the goaf accepts more load from overlying strata, leading to a lower side abutment pressure.

There are three main roadway layouts for the lower slice – inner offset, overlapped, and outer offset, as shown in Figure 3b. Inner offset gateroads of the lower slice are located under the stabilized goaf of the upper slice, which is in a destressed state (Peng, 2006). The mine planned to use inner offset gateroads for the lower slice in order to reduce the pressure on gateroads and the support cost. The original support design was steel yieldable I-beam sets together with mesh during extraction of the upper slice. The drawbacks of MLM are the low recovery due to the coal remaining in the pillar and repeated disturbance of the goaf, which increases the risk of spontaneous combustion.

For HSLM (Figure 3c), there is a curved section on either end of the goaf where the mining height decreases. This leads to localized higher goaf pressure within the sections.
compared with CLTCC. For the remainder of the goaf, the pressure is similar to that in CLTCC. Since the load borne by two ends of the goaf increases, the abutment pressure is less than that in CLTCC, but higher than that when mining the upper slice in MLM.

For LMSG, as only one end of the panel has an elevating section, the pressure distribution for the two ends of the panel is different: the goaf pressure at the curved section is larger than that of the other side, and while the abutment pressure on the left side is similar to that in CLTCC, that on the other side is similar to HSLM, as shown in Figure 3d.

According to the analysis above, the corresponding theoretical stress distribution for four mining methods is given below.

**Physical modelling**
In physical modelling, the goaf pile configuration and roof strata break or subsidence can be displayed visually. Strata movement can be measured by photogrammetry. Strain gauge indicators and stress sensors are used to collect stress-strain data. The accuracy of physical modelling has been improved in the past two decades by external observation and measurement methods including optical measurement, digital image correlation (DIC), photogrammetry, etc. (Ghabraie et al., 2015; Weishen et al., 2011).

In this investigation, physical modelling was used to observe the strata movement characteristics and goaf development process. Due to the limitation of the experimental period, only two experiments were carried out, one on CLTCC and the other on LMSG. Two large-scale physical models, using plane-stress simulation, were developed. The model dimensions were length 162 cm, height 130 cm, and thickness 16 cm (Figure 6). A strain gauge indicator was installed along the floor to monitor the stress. For physical model development, a geometric scaling factor $C_L$ (the ratio of model size to prototype size) of 1:100, and density factor $C_D$ (the ratio of model density to prototype density) 1:1.5 were used (Weishen et al., 2011). Key steps and results are shown in Figure 7.

From Figure 7 we can see, firstly, that the clutter of the arrangement of the caved rocks reduces in ascending order. In addition, the caved rocks on the right end of the LMSG panel are in better order than that on the left end (Figure 7g). Physical modelling indicates the basic goaf development configurations. But note that physical modelling cannot represent the *in situ* situation perfectly. For instance, the sizes of the caved rocks (especially those from immediate roof) are much larger than those in reality.
Interaction between vertical stress distribution within the goaf and surrounding rock mass

Secondly, caving lines are developed on two sides of the panel with the cave-in process of the overlying strata as shown in Figure 7(e). The angle of break is defined as the acute angle formed by the caving line and coal seam bedding plane. Thus, according to the geometrical relationship and key stratum theory (Qian, Shi, and Xu, 2010), the span 2L2 of the lower key stratum surpasses its limit caving interval and caves in, while the uppermost key stratum does not fracture, meaning that span 1L1 is at less than its limit caving interval. Therefore, angle of break has a significant influence on the fracturing and caving of overlying strata. For a panel with a certain width, after extraction of the panel, the larger the angle of break, the easier the fracture of an overlying stratum, and then the load of the caved stratum is borne by the goaf. On the other hand, if the angle of break is relatively large, the span of an overlying stratum cannot reach the limit caving interval, and then the load would transfer to the abutment due to its overhang above the goaf. It is evident that angle of break has a significant influence on stress distribution after the extraction of a panel, for both the goaf and the abutment, which would also affect numerical modelling. Most of the numerical modelling exercises to date have not incorporated the angle of break or have used an angle of break of 90°, which is not reasonable. The angles of break in physical modelling were obtained by using photogrammetry and were incorporated into the subsequent numerical modelling. The angles of break on the left and right in CLTCC are 60.9° and 58.7°, respectively. The angles of break on the left and right in LMSG are 59.6° and 62.7°, respectively. An angle of 60° was used in the numerical modelling so as to reduce the difficulty in establishing models.

Stress-strain data is translated into stress data and is shown in Figure 8. As a conventional rectangular pillar is eliminated by employing LMSG, the panel width is 10 m larger than that in CLTCC (Figure 1), thus the span of overlying strata in LMSG is greater than that in CLTCC, leading to the fracturing of higher strata. From the left end to the middle, the goaf pressures of the two methods are almost the same. On the right of the goaf, due to the curved section, the goaf pressure is near zero. With the development of the finite-difference program FLAC3D, the built-in double-yield constitutive modelling capability can be used to simulate the goaf behaviour, as the double-yield constitutive model represents a material in which there may be significant irreversible compaction in addition to shear yielding, such as hydraulically placed backfill or lightly cemented granular material (Itasca, 2012).

It is evident that in the initial stage of the numerical modelling, the goaf cannot bear the weight of the overlying strata, which fracture and subside, and this in turn gradually compacts the goaf, leading to the increase in the volumetric strain. The stress and volumetric strain are related as proposed by Salamon (Equation [2]). When the pressure borne by the goaf increases to a point when overlying strata do not subside further, a new equilibrium is reached and stress is redistributed both in the goaf and the surrounding rock mass. A reasonable numerical model is expected to demonstrate the above process.

Numerical modelling

The Mohr–Coulomb constitutive model is used for the rock strata modelling, and the double-yield constitutive model for the goaf modelling. The key to the success and practicality of numerical analysis for longwalls is goaf modelling, which is difficult due to the unavailability of goaf material parameters and difficulties in determining proper constitutive models. Very few studies have been conducted on goaf material. Pappas and Mark, (1993) studied the compaction characteristics of goaf material through laboratory tests, and indicated that Salamon’s model agrees well with the laboratory results. Salamon’s model (Salamon, 1990) is:

$$\sigma = \frac{E_0 \epsilon}{1 - e/e_m}$$

where \(\sigma\) is the uniaxial stress applied to the material (MPa), \(\epsilon\) is the strain occurring under the applied stress, \(E_0\) is the initial tangent modulus (MPa), and \(e_m\) is the maximum possible strain of the bulked rock material.

Yavuz (2001) carried out a study on the goaf pressure re-establishment, which also involves goaf behaviour, and derived the following relationship:

$$E_0 = \frac{10.39 \sigma_i^{0.412}}{b^{7.7}}$$

where \(\sigma_i\) is the compressive strength of the rock pieces within the goaf, [MPa]. \(e_m\) is related to the initial bulking factor \(b\) of goaf material (Peng, 1978):

$$e_m = \frac{b - 1}{b}$$

The initial bulking factor \(b\) is given as:

$$b = \frac{H_c + m}{H_c}$$

where \(H_c\) is the height of the caved zone (m).

With the development of the finite-difference program FLAC3D, the built-in double-yield constitutive modelling capability can be used to simulate the goaf behaviour, as the double-yield constitutive model represents a material in which there may be significant irreversible compaction in addition to shear yielding, such as hydraulically placed backfill or lightly cemented granular material (Itasca, 2012).

Figure 8—Stress distribution obtained from physical modelling
Interaction between vertical stress distribution within the goaf and surrounding rock mass

The numerical models were built based on Figure 1. For convenience of comparison, a model in which the goaf was assigned as null was also built. All the models were 400 m long, 90.8 m high, and 300 m thick. The LMSG model is shown in Figure 9. A vertical pressure of 79.1 m × 0.027 MN/m² (4.84 MPa) was applied on the top of the model to simulate the overburden weight. The horizontal sides were roller-constrained and the bottom boundary was fixed both horizontally and vertically. The horizontal-to-vertical stress ratio was set to be 1.2 in the x and y directions according to in situ data. A bedding plane was simulated by inserting interfaces between different strata using the same parameters as in the literature (Wang, Wang, and Yang, 2017). Rock mass engineering parameters are given in Table I. Now the key is to determine the properties for the goaf.

According to the engineering data for the 22202 panel, the height of the caved zone is about 16.9 m above the coal seam, according to observations in the goaf gas boreholes which reach to the 5.8 m thick siltstone stratum. Hence, the bulking factor was calculated to be \( b \approx 1.3 \) and the maximum strain of the goaf was calculated as \( \varepsilon_{\text{max}} = 0.23 \text{ m/m} \).

In order to obtain the parameters for the goaf and make sure that the strain-stress relationship satisfies Equation [6], a simple model with dimensions 1 m (length) × 1 m (width) × 2 m (height) was built. Loading was simulated by applying a velocity on the top surface with the bottom surface and four side surfaces fixed. The input parameters were fitted by an iterative change in the bulk and shear moduli, the angle of dilation, the angle of friction, and the density of the goaf material. By trial and error, the final properties in Table III were arrived at. The volumetric strain, vertical stress contours, and stress-strain matching curves of the two methods are shown in Figure 10. The numerical modelling results for CLTCC are shown in Figure 11.

### Table II
**Cap pressures for the double-yield model**

<table>
<thead>
<tr>
<th>Strain (m/m)</th>
<th>Stress (MPa)</th>
<th>Strain (m/m)</th>
<th>Stress (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.01</td>
<td>0.51</td>
<td>0.12</td>
<td>12.36</td>
</tr>
<tr>
<td>0.02</td>
<td>1.07</td>
<td>0.13</td>
<td>14.76</td>
</tr>
<tr>
<td>0.03</td>
<td>1.69</td>
<td>0.14</td>
<td>17.69</td>
</tr>
<tr>
<td>0.04</td>
<td>2.37</td>
<td>0.15</td>
<td>21.37</td>
</tr>
<tr>
<td>0.05</td>
<td>3.13</td>
<td>0.16</td>
<td>26.14</td>
</tr>
<tr>
<td>0.06</td>
<td>3.98</td>
<td>0.17</td>
<td>32.53</td>
</tr>
<tr>
<td>0.07</td>
<td>4.93</td>
<td>0.18</td>
<td>41.56</td>
</tr>
<tr>
<td>0.08</td>
<td>6.02</td>
<td>0.19</td>
<td>55.34</td>
</tr>
<tr>
<td>0.09</td>
<td>7.26</td>
<td>0.20</td>
<td>78.83</td>
</tr>
<tr>
<td>0.10</td>
<td>8.70</td>
<td>0.21</td>
<td>128.00</td>
</tr>
<tr>
<td>0.11</td>
<td>10.37</td>
<td>0.22</td>
<td>295.40</td>
</tr>
</tbody>
</table>

### Table I
**Rock mass engineering parameters used in numerical modelling**

<table>
<thead>
<tr>
<th>Lithology</th>
<th>Thickness (m)</th>
<th>Depth (m)</th>
<th>Density (kg/m³)</th>
<th>Cohesion (MPa)</th>
<th>Friction angle (°)</th>
<th>Tensile strength (MPa)</th>
<th>Bulk modulus (GPa)</th>
<th>Shear modulus (GPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Top</td>
<td>20.0</td>
<td>179.1</td>
<td>2600</td>
<td>8.5</td>
<td>44.2</td>
<td>2.2</td>
<td>21.4</td>
<td>14.5</td>
</tr>
<tr>
<td>Mudstone</td>
<td>5.4</td>
<td>199.1</td>
<td>2550</td>
<td>3.6</td>
<td>33.9</td>
<td>1.6</td>
<td>10.4</td>
<td>9.7</td>
</tr>
<tr>
<td>Silty mudstone</td>
<td>6.4</td>
<td>204.5</td>
<td>2550</td>
<td>5.5</td>
<td>36.7</td>
<td>1.56</td>
<td>12.0</td>
<td>11.3</td>
</tr>
<tr>
<td>Siltstone</td>
<td>5.8</td>
<td>210.9</td>
<td>2540</td>
<td>8.2</td>
<td>43.9</td>
<td>2.1</td>
<td>19.8</td>
<td>13.4</td>
</tr>
<tr>
<td>Medium-grained sandstone</td>
<td>3.2</td>
<td>216.7</td>
<td>2550</td>
<td>6.9</td>
<td>41.2</td>
<td>1.6</td>
<td>17.8</td>
<td>11.2</td>
</tr>
<tr>
<td>Coal</td>
<td>1.5</td>
<td>219.9</td>
<td>1400</td>
<td>1.2</td>
<td>27.1</td>
<td>0.6</td>
<td>6.9</td>
<td>4.2</td>
</tr>
<tr>
<td>Sandy mudstone</td>
<td>6.4</td>
<td>221.4</td>
<td>2550</td>
<td>5.5</td>
<td>36.7</td>
<td>1.56</td>
<td>12.0</td>
<td>11.3</td>
</tr>
<tr>
<td>Fine-grained sandstone</td>
<td>2.2</td>
<td>227.8</td>
<td>2550</td>
<td>8.4</td>
<td>44.5</td>
<td>2.4</td>
<td>22.2</td>
<td>14.8</td>
</tr>
<tr>
<td>2-4 coal</td>
<td>5.0</td>
<td>230.0</td>
<td>1400</td>
<td>1.2</td>
<td>27.1</td>
<td>0.6</td>
<td>6.9</td>
<td>4.2</td>
</tr>
<tr>
<td>Sandy mudstone</td>
<td>3.1</td>
<td>235.0</td>
<td>2550</td>
<td>4.8</td>
<td>35.7</td>
<td>1.7</td>
<td>11.6</td>
<td>10.5</td>
</tr>
<tr>
<td>Siltstone</td>
<td>4.2</td>
<td>238.1</td>
<td>2540</td>
<td>8.2</td>
<td>43.9</td>
<td>2.1</td>
<td>19.8</td>
<td>13.4</td>
</tr>
<tr>
<td>Sandy mudstone</td>
<td>4.8</td>
<td>242.3</td>
<td>2550</td>
<td>4.8</td>
<td>35.7</td>
<td>1.7</td>
<td>11.6</td>
<td>10.5</td>
</tr>
<tr>
<td>Mudstone</td>
<td>5.0</td>
<td>247.1</td>
<td>2550</td>
<td>3.6</td>
<td>33.9</td>
<td>1.6</td>
<td>10.4</td>
<td>9.7</td>
</tr>
<tr>
<td>Bottom</td>
<td>20.0</td>
<td>252.1</td>
<td>2600</td>
<td>8.5</td>
<td>44.2</td>
<td>2.2</td>
<td>21.4</td>
<td>14.5</td>
</tr>
</tbody>
</table>
From Figure 11 it is clear that the stress concentration areas are developed within the surrounding rock mass on two sides of the goaf. Within these zones, the upper strata are subjected to higher stress concentration than the lower strata, which shows the effect of the goaf on spatial abutment pressure distribution. The simulated floor stress monitoring results are shown in Figure 11e. Goaf pressure increases gradually from zero at the goaf edge and peaks in the middle of the goaf. It can be seen that the failure develops in the same direction as the caving line in tensile and shear failure modes; the middle of the overlying strata above the goaf shows tensile failure (three red ellipses in Figure 11c). This demonstrates that the middle of the overlying strata above the caved zone and close to the caving line undergoes tensile failure, resulting in corresponding destressed zones around them (the three ellipses in Figure 11a). Failure development within the floor strata is also along the direction of the caving line. The area of the yield zone within the coal seam increases from top to bottom.

In order to show the reasonableness of considering the goaf, numerical modelling ignoring the goaf was also carried out. The results are shown in Figure 12.

During the running of FLAC3D, if the goaf is not taken into account, i.e., a null is assigned to the goaf, the maximum unbalanced force cannot reach the default value of $1 \times 10^{-5}$, remaining around $4.4 \times 10^{-4}$, which means the equilibrium results are not ideal. Figure 12 shows that although the roof subsides somewhat, it still overhangs the 130 m wide extracted area. Thus the roof is in a tensile state, which is not realistic. In reality, the roof strata will subside and touch the goaf, hence there would also be compressive stress; the concentrated stress appears mainly within the abutment of the coal seam and the two rock strata overlying the seam. Compared with numerical modelling results incorporating the goaf effect, the following differences are apparent:

- The stress concentrated abutment pressure zone (dark blue zones in Figure 12b) is lower
- The height of the zone decreases
- The stress concentration factor is larger, and the zone traverses the coal seam

Interaction between vertical stress distribution within the goaf and surrounding rock mass

Figure 11—Numerical modelling results for conventional longwall mining

Figure 10—Iteratively fitted results
Interaction between vertical stress distribution within the goaf and surrounding rock mass

The areas of floor and roof failure and the yield zone are larger.

The surface subsidence is larger.

The floor stress monitoring indicates that the floor stress is zero, which is totally unreasonable.

Most importantly, as there is no goaf material in Figure 12, the support force against the overlying strata is zero, which leads to higher abutment pressure. From Figure 12d, the abutment pressure peak is 22.5 MPa, thus the stress concentration factor is $22.5 \div 6.2 = 3.63$; while for modelling with goaf material, abutment pressure peak is 21.8 MPa, and stress concentration factor is $20.0 \div 6.2 = 3.23$. It is evident that the effect of the goaf cannot be ignored, or unreasonable modelling results would ensue. The modelling results for LMSG are shown in Figure 13.

Figure 13 shows that failure of the overlying strata develops in the same direction as the caving line in tensile and shear failure modes. Tensile failure (green elements) occurs in the middle of the overlying strata, which demonstrates that the middle of the strata overlying the

Figure 12—Numerical modelling results without taking goaf effect into account

Figure 13—Numerical modelling results for split-level longwall mining
caved zone and close to the caving line undergoes tensile failure, resulting in corresponding destressed zones around these areas. The curved section is located in the destressed zone under the caving line. Meanwhile, the yield zone around the curved section is smaller and the integrity of the coal mass is better. Therefore, the gateroads located here would suffer lower ground pressure and ground control problems would be consequently be minimum. According to records for several panels in Zhenchengdi coal mine, dynamic disasters such as rockbursts or coal and gas outbursts have never occurred under such conditions.

The stress monitoring results (Figure 13e) indicate that the abutment pressure on the side of the curved section is smaller and the goaf pressure increases faster. This means that the load borne by the goaf is larger, especially for the curved section (red circle in Figure 13e). The modelling results for HSLM are shown in Figure 14.

As the mining height is approximately the same with CLTCC, the yielded zone is also approximately the same. However, as there are curved section on either side of the panel, the goaf pressures within these sections are larger (blue areas), which leads to slightly lower abutment pressure than that in CLTCC.

As the goaf configuration in MLM is different, different goaf parameters need to be determined. The process is similar to that for LMSG. As the mining height for the upper single slice in MLM is lower, the height of caved zone must consequently be lower. The caved zone involves two overlying strata directly above the coal seam; that is, 2.2 m fine sandstone and 6.4 m sandy mudstone. Thus, the height of the cave zone above coal seam is 8.6 m. The upper slice is 2.5 m, thus it is calculated that $b = 1.3$, and maximum strain $\epsilon_{\text{max}} = 0.225$ m/m. Still taking $\sigma_c = 50$ MPa, hence $E_0 = 50.4$ MPa, the expression for cap pressure (Table IV) is therefore:

$$\sigma = \frac{50.4\varepsilon}{1 - 4.44\varepsilon}$$

A simple model with dimensions 1 m (length) × 1 m (width) × 2 m (height) was also built to obtain the reasonable parameters for the goaf and ensure that the strain-stress relationship satisfies Equation [7]. Loading was simulated by applying a velocity on the top surface with the bottom surface and four side surfaces fixed. The input parameters were fitted by an iterative change in the bulk and shear moduli, the angle of dilation, the angle of friction, and

<p>| Table IV |
|------------------|------------------|------------------|------------------|
| <strong>Cap pressure for the double-yield model</strong> | | | |</p>
<table>
<thead>
<tr>
<th>Strain (m/m)</th>
<th>Stress (MPa)</th>
<th>Strain (m/m)</th>
<th>Stress (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.01</td>
<td>0.63</td>
<td>0.12</td>
<td>12.95</td>
</tr>
<tr>
<td>0.02</td>
<td>1.11</td>
<td>0.13</td>
<td>15.50</td>
</tr>
<tr>
<td>0.03</td>
<td>1.74</td>
<td>0.14</td>
<td>18.65</td>
</tr>
<tr>
<td>0.04</td>
<td>2.45</td>
<td>0.15</td>
<td>22.83</td>
</tr>
<tr>
<td>0.05</td>
<td>3.24</td>
<td>0.16</td>
<td>27.85</td>
</tr>
<tr>
<td>0.06</td>
<td>4.12</td>
<td>0.17</td>
<td>34.94</td>
</tr>
<tr>
<td>0.07</td>
<td>5.12</td>
<td>0.18</td>
<td>45.18</td>
</tr>
<tr>
<td>0.08</td>
<td>6.25</td>
<td>0.19</td>
<td>61.22</td>
</tr>
<tr>
<td>0.09</td>
<td>7.55</td>
<td>0.20</td>
<td>90.00</td>
</tr>
<tr>
<td>0.10</td>
<td>9.06</td>
<td>0.21</td>
<td>156.57</td>
</tr>
<tr>
<td>0.11</td>
<td>10.84</td>
<td>0.22</td>
<td>477.92</td>
</tr>
</tbody>
</table>

Figure 14—Numerical modelling results for high-seam longwall mining
Interaction between vertical stress distribution within the goaf and surrounding rock mass

the density of the goaf material. By trial and error, the final properties in Table V were obtained. The volumetric strain, vertical stress contours, and stress-strain matching curves of the two methods are shown in Figure 15. The numerical modelling results for MLM are shown in Figure 16.

It is demonstrated in Figure 16 that the stress concentration areas (dark blue areas) are smaller than with any other method. Due to the effect of the goaf, the stress concentration areas are located above, and not within, the coal seam as many numerical modelling exercises have concluded in the past. The yielded zone is smaller than with any of the other methods. In addition, the caved zone above the coal seam is 8.6 m, which is smaller than for any other methods, thus leading to a greater degree of compaction. The peak abutment pressure is 20 MPa, which is lower than for any other methods. The results verify the conclusion from the theoretical analysis, that the goaf and surrounding rock mass affect each other: thus, the more load the goaf bears, the lower the abutment pressure, and vice versa. As mining of the lower slice leads to the same mining height as with CLTCC, the modelling process is the same as for CLTCC and therefore is not included here.

Field trial and observations

The study so far suggests that the goaf edge is the area where pressure is smallest in the entire longwall panel system, therefore the ground control problems here are minimum. The research results guided the choice of LMSG for practical application for mining of the panels. The 3D views of the shields of 22204 LMSG panel are shown in Figure 17. The unique panel configuration requires unique mining operations (triple sections mining technology) which is different from other methods (Zhao, 2004; Qiao, 2015).

<p>| Table V: Goaf parameters for multiple-slice longwall mining |</p>
<table>
<thead>
<tr>
<th>Density (kg/m³)</th>
<th>Bulk modulus (MPa)</th>
<th>Shear modulus (MPa)</th>
<th>Angle of dilation (°)</th>
<th>Angle of internal friction (°)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1700</td>
<td>80E9</td>
<td>60E9</td>
<td>8.2</td>
<td>4</td>
</tr>
</tbody>
</table>

Figure 15—Iteratively fitted results

![Figure 15](image1)

Figure 16—Numerical modelling results for multi-slice longwall mining

![Figure 16](image2)
The field observations, including measurements of stress and roadway deformation, were carried out when the face was about 300 m away from set-up room of 22204 panel. For comparison, the same data for the 22202 intake entry and gas-drainage entry was also obtained during the extraction of 22202 panel. Figure 18 shows the support design and the variation in stress and convergence with distance from the face.

Figure 18b shows that roof stress of 22202 gas-drainage entry is slightly lower than that of 22202 intake entry, which indicates the interaction between goaf and surrounding rock mass, i.e., the mining height is lower at the curved section, thus leading to a higher load borne by the goaf, which results in lower roof pressure in the intake entry than the gas-drainage entry on the other side (without a curved section). Furthermore, the roof pressure of 22204 intake entry remains under 1 MPa; this is believed to be due to 22204 intake entry being located under the edge of the goaf of 22202 panel. As aforementioned, the edge of the goaf is the most destressed area in the entire longwall system. Therefore, 22204 intake entry is independent of abutment pressure. The principle is similar to the roadways for lower slices of MLM located directly within the destressed zone, that is, the goaf resulting from the upper slice.

Figure 18 shows that the roof-to-floor convergence for all three roadways increases with increasing distance to the working face, but this is not obvious for 22204 intake entry. Roof-to-floor convergence of 22202 intake entry is the largest, because the location of the entry is the same as in CLTCC and the roof consists of coal. The 22202 gas-drainage entry has a relatively smaller roof-to-floor convergence, because the entry is located along the immediate roof of rock, which is much more competent than a coal roof. In addition, due to the interaction between the goaf and surrounding rock mass, the pressure around the entry is smaller, leading to smaller roof-to-floor convergence. According to the records of the mine, during the extraction of 22204 panel, no roof falls occurred. The roof-to-floor convergence of 22202 intake entry is the smallest, and the deformation of the roof results only from sagging due to gravity. Rib-to-rib convergence of 22202 intake entry is larger than for 22202 gas-drainage entry and that of 22204 intake entry, which is the smallest. Rib-to-rib convergences of 22202 intake entry and 22202 gas-drainage entry increase with distance to the working face, while that of 22204 intake entry does not show any relationship with the distance to the working face, demonstrating that the entry is independent of abutment pressure. This indicates that the stress environment is favourable for entry support and maintenance. The field observations demonstrate that the support design shown in Figure 18a ensured the safe operation of the entry, and mining took place without any accidents.

The approach is best adopted for coal seams with thicknesses greater than 5 m (Wang et al., 2017). LMSG has also been used for coal seams with dip angles of more than 52° (Zhao, Wang, and Su, 2017). As has been shown in this paper, the two adjacent panels must be extracted sequentially, and the entry of the future panel should be developed at least 6 months (preferably one year) after the previous panel has been mined. This point must be kept in mind when a coal mine plans to use the approach. LMSG is preferred for coal seams with a low gas content. With appropriate measures, such as pre-drainage of gas through goaf boreholes or cross-measure drill-holes, grouting, employing an outer offset configuration with a slender pillar in between, or leaving a coal layer between two adjacent panels as shown in Figure 19, LMSG can also be applied for coal seams with a higher gas content. The author has proposed the use of geosynthetic materials (such as membranes) to isolate the entry from the goaf (Editorial Committee, 2015), which is the topic of further research. Furthermore, coal mines currently using LMSG are all single-entry or two-entry systems and are found only in China and Russia. Therefore, additional research should be done to assess the performance of panel systems with three, four, and more entries.

Applicability of the approach

The approach is best adopted for coal seams with thicknesses greater than 5 m (Wang et al., 2017). LMSG has also been used for coal seams with dip angles of more than 52° (Zhao, Wang, and Su, 2017). As has been shown in this paper, the two adjacent panels must be extracted sequentially, and the entry of the future panel should be developed at least 6 months (preferably one year) after the previous panel has been mined. This point must be kept in mind when a coal mine plans to use the approach. LMSG is preferred for coal seams with a low gas content. With appropriate measures, such as pre-drainage of gas through goaf boreholes or cross-measure drill-holes, grouting, employing an outer offset configuration with a slender pillar in between, or leaving a coal layer between two adjacent panels as shown in Figure 19, LMSG can also be applied for coal seams with a higher gas content. The author has proposed the use of geosynthetic materials (such as membranes) to isolate the entry from the goaf (Editorial Committee, 2015), which is the topic of further research. Furthermore, coal mines currently using LMSG are all single-entry or two-entry systems and are found only in China and Russia. Therefore, additional research should be done to assess the performance of panel systems with three, four, and more entries.
Interaction between vertical stress distribution within the goaf and surrounding rock mass

Conclusions
The equilibrium state after the extraction of longwall panels is the result of interaction between the goaf and the surrounding rock mass. Theoretical analysis, physical modelling, numerical modelling, and field observation were carried out for 22202 and 22204 panels in Zhenchengdi coal mine to study the interaction. The main conclusions are as follows.

1. Different panel layouts lead to different panel configurations and surrounding rock pillar or rock mass configurations. Goaf behaviour has a significant influence on stress distribution and failure of the surrounding coal and rock mass, and should not be ignored in numerical modelling.

2. Physical modelling of the movement of overlying strata and goaf development shows that caving lines and angles of break develop, and the angle of break has an important influence on goaf, coal, and rock mass configurations as well as stress distribution. The angle of break was used for numerical modelling. Stress monitoring indicates that the abutment pressure on the side with the curved section of the HSLM panel is lower than that in CLTCC, while the pressure distributed within the curved section is higher than that of CLTCC.

3. A double-yield constitutive model was used to simulate goaf material. The results show that different goaf configurations lead to different stress distributions; the greater the load that the goaf bears, the less the abutment pressure and vice versa. The abutment pressure, stress concentration factor and yield zone would be larger and the elevation of yield zone would be lower without taking the influence of goaf into account. Goaf pressures of curved section employing HSLM and LMSG are larger than those in MLM and CLTCC. When one slice is being extracted in MLM, the goaf pressure is larger than for any other method, and the stress concentration zone and yield zone are smaller. Critical panel widths are not reached for all methods. For all methods, the stress of the goaf edge is the smallest in the entire panel system.

4. Field monitoring indicates that the front abutment pressure within the entry on the side of the panel with a curved section (gas-drainage entry) is higher than that of the entry on the other side (intake entry). The goaf edge is the most destressed and energy-released zone, and the entry is independent of abutment pressure.

LMSG is helpful for extraction of coal deposits experiencing high ground pressure or related problems, and the technology has potential to be utilized in many countries worldwide. Interested parties may contact the authors regarding the intellectual property and application of the technology in different settings.

Acknowledgement
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Advanced ceramics – the new frontier in modern-day technology: Part I
by W.R. Matizamhuka

Synopsis
Advanced ceramics have demonstrated phenomenal performance under severe conditions in a number of areas, including wear-related applications, transport, energy and environment, health, high-temperature, and electronic applications. However, they have yet to attain the long-awaited broad market penetration, especially on the African continent. The growth of advanced ceramics usage has been hindered mainly by low reliability, brittleness, unfamiliarity to potential users, redesign requirements, and the high cost of components. Research and development has led to significant improvements in the properties of advanced ceramics. The paper features some background on the development of advanced ceramics, and a review of the properties and application areas, with the aim of providing insight into the potential areas in this field where the South African science and engineering community could invest time and resources.

Keywords
advanced ceramics application, engineering materials, superior performance, technological applications.

Introduction
Advanced ceramics are an integral part of modern technology. Most of these products play crucial functions ‘behind the scenes’ in a number of applications in everyday life. They usually offer superior performance that cannot be replicated easily by other materials (Riedel, 2013). Advanced ceramics today play a key role in technologies such as energy and the environment, transport, the life sciences, and communication and information technology (Greil, 2002).

The terminology for defining this type of ceramics differs from continent to continent (Kulik, 1999). In the Japanese literature it’s normally referred to as ‘fine’ ceramics, and in American literature as ‘advanced’ or ‘technical’ ceramics (Kulik, 1999). In the European context the term ‘technical’ ceramics is more frequently used (Kulik, 1999). A further classification, depending on the use, is common in the UK, where the term ‘technical ceramics’ is further subdivided into ‘functional ceramics’ to refer to electronic applications and ‘structural ceramics’ to refer mostly to mechanically loaded components (Kulik, 1999).

Advanced ceramics possess unique properties that cannot be obtained in conventional materials, such as high refractoriness and hardness, low density, low coefficient of thermal expansion (CTE), and higher working temperatures (can maintain good mechanical properties at high temperatures). Moreover, there are reports which have proven that the cost of producing ceramic materials is lower compared to metallic materials, and raw material reserves for ceramics are abundant (Kulik, 1999). Resources for the production of metals and their alloys are dwindling, and the continuously increasing demand for engineering products requires alternative materials to be identified. Over the past few decades advanced ceramics have made inroads in a number of critical applications in everyday life. It is noteworthy to mention here that without sparkplugs made of alumina (Al₂O₃) ceramic, vehicle technology would not be so advanced, moreover metallurgy would not be so reliable without refractories (Kulik, 1999). These are the hard facts behind commonplace products that we normally take for granted.

Although ceramics play a crucial role in a number of technologies due to their unique combination of properties, it must be noted that as structural materials they still face stiff competition from cheap metals, alloys, and composites (Kulik, 1999). Thus the major barriers to the broad application of advanced ceramic materials include the lack of specifications and databases, high scale-up costs, and lack of repair methods (Freitag and Richerson, 1998). However, over the years a lot of progress has been made to alleviate these deficiencies through new material discoveries, improvements in properties, and improved design methods (Freitag and Richerson, 1998).
Advanced ceramics — the new frontier in modern-day technology: Part I

World market for advanced ceramics

The term ‘advanced ceramics’ was coined in the 1970s to designate a new category of engineering materials that were to drive new technologies into the 21st century (Charreyron, 2013). Since then there has been phenomenal growth in the technological advancement of these materials. A report from Research and Markets projected the advanced ceramics market to reach US$10.4 billion by 2021, growing at a compounded annual growth rate (CAGR) of 6.5% (Charreyron, 2013). This growth is attributed to the increasing use of advanced ceramic materials as alternatives to metals and plastics, with key drivers being the medical, electronics, and transport industries. The analog-to-digital shift in consumer products has seen massive growth in electronic device content in a number of applications. For instance, liquid crystal displays (LCDs) replaced cathode ray tubes and DVDs replaced VHS tapes and players. This basically points to significant growth for ceramic capacitors and other ceramic electronic components. The largest share of the market has always been in the electronics industry, representing approximately more than 70% of production, but positive and negative shifts are expected according to changes in demand (Kulik, 1999).

Advanced ceramics are produced from three main classes of materials, namely oxides, carbides, and nitrides, with a small quantity accounting for mixed compounds (World Advanced Ceramics, 1966). Japan has been at the forefront for a number of years, owing partly to the high degree of cooperation between companies in investigations and developments (dynamic partnership) and high export volumes (Kulik, 1999; Charreyron, 2013). The major volume of production in Japan is represented by electronic ceramics, accounting for up to 80% of total production (Kulik, 1999). The second largest producer of advanced ceramics is North America, where the industry has been driven by massive government financing of research and design development. The main difference between the two approaches is that North America plays a leading role in technology and Japanese companies lead in the applications of advanced ceramics. Such approaches have been successfully adopted by a number of European countries that now contribute extensively to the advanced technology market. One such country is Germany, which is home to a number of companies that compete for advanced technology projects throughout the world.

Advances in research and development of advanced ceramics

One of the most significant advances in ceramics research in the past two decades has been improvements in fracture toughness, especially for structural ceramics. On a comparative basis, glass has a fracture toughness of 1 MPa.m^{0.5} and most conventional ceramics range from about 2–3 MPa.m^{0.5}; steel is about 40 MPa.m^{0.5} (Freitag and Richerson, 1998). Some advanced ceramics such as transformation toughened zirconia-ZrO_{2} have toughness of about 15 MPa.m^{0.5}, which is higher than that of tungsten-carbide cobalt (WC-Co) cermet and cast iron (Freitag and Richerson, 1998). This has dramatically improved the resistance to contact stress and handling damage, thus imparting high reliability and durability comparable to that of metals and WC-Co cermets (Freitag and Richerson, 1998). Prior to 1970, most ceramic materials had strengths well below 345 MPa, but nowadays advanced ceramics such as silicon nitride (Si_{3}N_{4}) and toughened zirconia (ZrO_{2}) are commercially available with strengths above 690 MPa (Freitag and Richerson, 1998).

The detailed mechanism of transformation toughening can be found elsewhere (Matizamhuka, 2016). However, what is important to note is that fracture toughness values 3–6 times higher than monolithic ZrO_{2} ceramics have been achieved by transformation toughening. Several other techniques have been developed over the years to improve fracture toughness of advanced ceramics, such as the use of more ductile binders and reinforcement with fibres, whiskers, or second-phase particles. Details of such techniques can be found in the open literature (Matizamhuka, 2016).

On the other hand, the high cost of ceramic components has been attributed to the lack of large-scale production with minimum losses in the production line. Ceramic-based materials often compete against engineering materials with lower upfront costs, and it is often difficult to convince customers to pay a premium in exchange for performance benefits (Charreyron, 2013). Design, process technology, and machining technology still need to develop significantly to achieve cost-effective levels of high-volume production, consequently reducing the cost of components. A strategy used by previous market pioneers is that of forward pricing and continued government subsidies in anticipation of future market growth. The recent phenomenal growth in the advanced ceramics industry could easily translate into a greater market share in future, but this can happen only if major breakthroughs are achieved in fundamental and applied research (Liang and Dutta, 2001).

It is often considered that most new technologies arise from fundamental research and can be disconnected from social or market needs (Charreyron, 2013). History has proven that it may take decades for basic science to be translated into applied research. However, the majority of new business development cycles actually start with the onset of applied research programmes (Charreyron, 2013). Furthermore, applied research becomes active only when government-sponsored research such as in public institutions (addressing socioeconomic needs) meets private research and development, which is profit-driven. New business development requires effective coordination of public and private research and development policies (Charreyron, 2013). Such efficient coordination is clearly exemplified by the industry roadmap of the Japanese Ministry of International Trade and Industry (MITI) for the advancement of advanced ceramic materials in non-military domains (Charreyron, 2013). Clearly, research in advanced ceramics is one strategic project that South Africa can easily pursue to fulfill some of her visions as stipulated in the government’s National Development Plan 2030 (National Planning Commission, 2013). South Africa is endowed with some of the world’s largest reserves of natural resources, some of which are used as raw materials in the development of advanced ceramics. However, there are a limited number of locally owned facilities (if any) within South Africa involved in the manufacture of advanced ceramic components. A clear strategy here is the development of downstream value-
Advanced ceramics – the new frontier in modern-day technology: Part I

addition manufacturing plants for advanced ceramic components, coupled with technology transfer through bilateral agreements with strategic partner countries with the requisite knowledge.

Applications of advanced ceramics

Superior material manufacturing technology and unique product attributes are crucial to the success of advanced ceramics. Advanced ceramics have demonstrated phenomenal performance under severe conditions in a number of applications, which include wear-related applications, transport, energy and the environment, health, high-temperature, and electronic applications. The most commonly used ceramic materials are alumina, zirconia, silicon nitride, silicon carbide, ferrite, and titanates. Below is a brief overview of some of the most common applications of advanced ceramics, highlighting how they have developed over the years. The list is not meant to be exhaustive but aims to give a qualitative review of the applications of the most popular advanced ceramic materials.

Wear-related applications

Advanced ceramics have surpassed cemented carbides in cutting tool applications due to their greater hot hardness and strength, which enables high-speed and efficient machining (Kuzler, 2012). Metal machining or finishing operations require a tool with a combination of properties such as high fracture toughness and hot hardness, thermal shock resistance, and chemical stability (Greil, 2002). In cutting applications, the key factors are the life of the cutting tool and the cutting speed (Freitag and Richerson, 1998). During cutting of hard metals such as cast iron and high-temperature superalloys, high temperatures are generated at the tool-workpiece interface, resulting in decreased tool life and cutting speed. Thus the use of advanced ceramics in cutting applications (Figure 1) is more cost-effective owing to the high removal rate during cutting, which increases production rates, decreases flank wear of the tool tips, and gives the capability to cut difficult-to-machine materials. The ideal machining temperatures for Ni-based superalloys of approximately 1200°C can only be accommodated by ceramic cutting tools (Kuzler, 2012). Traditionally-used WC-Co inserts wear rapidly at high temperatures, thus limiting the cutting speed to 121.6 m/min (Freitag and Richerson, 1998). However, the use of silicon nitride (Si$_3$N$_4$) has demonstrated cutting speeds as high as 1520 m/min at a depth cut of 5 mm and feed rate of 0.4 mm per revolution, under severe conditions with operating temperatures of up to 1100°C (Freitag and Richerson, 1998), with a dramatic effect on manufacturing output.

Alumina (Al$_2$O$_3$) has been the lowest cost high-performance ceramic for many years, owing to the large quantities produced. Alumina is mainly used for roughing and finishing applications of cast and gray irons (Kuzler, 2012). Si$_3$N$_4$ exhibits high fracture toughness and is used in interrupted machining applications, which are more prone to fracturing (Kuzler, 2012). The silicon carbide (SiC) family of materials is now well-established in the market. Some of the most attractive characteristics are superior wear resistance and high temperature capability compared to Al$_2$O$_3$ and Si$_3$N$_4$. However, SiC is more expensive than Al$_2$O$_3$ and has a lower toughness than Si$_3$N$_4$, thus it is not the optimum material for all wear applications.

A major breakthrough in the advanced ceramics industry was achieving high strength and toughness through microstructural manipulations, as mentioned earlier (Freitag and Richerson, 1998). Composite cutting tools such as SiC whisker-reinforced alumina (SiC$_x$/Al$_2$O$_3$) have been commercialized and have found a niche in machining difficult materials such as Ni-based superalloys in the gas turbine industry, as well as wear components in the ceramic/metal hybrid tooling and dies in the canning industry for cupping, drawing, ironing, and can necking. The main drivers of advanced ceramic inserts include greater application in the finishing of hard-to-machine materials after roughing with carbide tools (Kuzler, 2012). This requires tools of finer grain size (< 100 nm), which leads to fine cutting edges and improved tool wear resistance (Kuzler, 2012).

Transport industry

A number of commercial applications for advanced ceramics have been realized in the automotive industry. Several components manufactured from advanced ceramics are regarded as being more suited to meeting future emission regulations and improving fuel economy, owing to their lower weight, durability, and lower cost. There are a number of components being produced for the automotive industry, including Si$_3$N$_4$ fuel injector links and high-pressure pumping plunges, ZrO$_2$ injector metering, exhaust gas particle filters, bearings, and low-weight, high coefficient of friction ceramic composite brakes (Mandler, 2001; Gadow, 2001; Greil, 2002) (Figure 2). Ceramic composite brakes have been used in the luxury car market since 2001, and it is expected that more applications will emerge as costs are reduced (Greil, 2002).

A number of advantages have been realized through the use of advanced ceramic components such as ceramic multilayer piezoelectric actuators in high-pressure common rail (CR-series) fuel injection diesel engines (Greil, 2002). This has resulted in improved ignition operation and consequently reduced noise and decreased pollutant emissions (Greil, 2002). Another merit is the improved thermal efficiency from about 40–42% in conventional engines to about 65% for turbo diesel engines equipped with heat insulating ceramic components in the combustion chamber and exhaust manifold (Greil, 2002). There has been extensive use in the development of lightweight spacecraft capable of operating at...
temperatures up to 1600°C, and silicon-carbide-based composites have been used for this application (Greil, 2002).

Over the last few decades the use of diesel engines has been on the rise in the developed countries. This is attributed to the excellent efficiencies achieved by these engines. Diesel engines find wide use in heavy-duty vehicles, agricultural and mining machinery, and stationary equipment. Since the beginning of the 21st century, purification of diesel engine exhaust emissions has become a priority owing to environmental legislation. In the EU, for instance, it is mandatory for all new vehicles to be equipped with high-efficiency diesel particulate filters (DPFs, Figure 3) in combination with NOx reduction systems (Adler, 2005; Adler and Petasch, 2013). The driver behind this is the health concerns associated with fine and ultrafine diesel particulate matter (DPM) discharged in the atmosphere as a result of incomplete combustion of diesel fuel (Adler, 2005; Adler and Petasch, 2013). Moreover, soot particles emitted from diesel engines have been found to contribute directly to the ‘greenhouse effect’, having 2000 times the effect of CO₂ (based on mass) (Adler, 2005; Adler and Petasch, 2013). The materials used in the manufacture of DPFs must possess certain desirable properties such as mechanical stability at high temperatures, chemical resistance, low Young’s modulus, low coefficient of thermal expansion (CTE), and high thermal conductivity.

Silicon carbide wall flow filters have been used in light motor vehicles since the year 2000 (Adler, 2005; Adler and Petasch, 2013). By the end of 2006 approximately 6–9 million silicon carbide filters were manufactured per year (Adler, 2005; Adler and Petasch, 2013). Aluminium titanate filters were introduced as a standard in passenger vehicles in 2006 (Adler, 2005; Adler and Petasch, 2013). On the other hand, cordierite has been extensively used in medium- to heavy-duty vehicles (Adler, 2005; Adler and Petasch, 2013). DPFs have become a billion dollar business in the few years since their inception and a bright outlook has been forecast, especially in Western Europe and North America (Adler, 2005; Adler and Petasch, 2013). Over the long term, the market for heavy-duty diesel engines is expected to rise, with the passenger vehicle market being dominated by alternative drive technologies, i.e. hybrid drives, fuel cells, and electric vehicles (Adler, 2005; Adler and Petasch, 2013).

The development of solid oxide fuel cells (SOFCs) as future sources for clean pollutant-free energy is a major breakthrough in the energy industry (Greil, 2002). SOFCs generate electricity and heat at high efficiencies with low levels of NOx and SOx emissions. (Greil, 2002). SOFCs utilize rapid ionic conductivity of either O₂⁻ or protons, which allows charge transport across a solid oxide electrolyte/membrane, thus generating electrical energy (Riedel, Ionescu, and Chen, 2008). The oxidation and reduction reactions occur on either side of the solid oxide membrane/electrolyte through the consumption of O₂ and fuel (H₂ or hydrocarbons) (see Figure 4).

The solid electrolyte is in the form of a ceramic with special properties to enable ionic conductivity; typically, yttria-stabilized zirconia with a Ni/ZrO₂ composite anode and LaMnO₃ cathode has been used. This has become a multi-million dollar industry with big conglomerates involved in the production of pressurized hybrid SOFC power generation systems up to 250 kW (Greil, 2002).

The use of advanced ceramics extends to battery technology for the construction of electrodes such as lithium oxides in Li-ion batteries (Nemoto, 2003). For practical purposes, the Li-ion battery offers greater advantages over other types owing to its light weight per unit stored energy, and high ionization potential, energy, and power densities. Lithium is normally used combined with transition metal

![Figure 2—An illustration of various ceramic components that are used in modern automobiles (Greil, 2002)](image)

![Figure 3—Diesel particulate filters (DPFs) made from SiC (left) and cordierite (right) produced by NGK Japan)](image)

![Figure 4—A schematic representation showing the working principle of a solid oxide fuel cell](image)
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oxides (due to the high reactivity of Li) coupled with a carbon electrode (Nemoto, 2003). Typically, LiCoO₂, LiMn₂O₄, LiFeO₂, and LiFePO₄ have been used as positive active ceramic materials to generate a voltage range of 3.5-4 V (Nemoto, 2003). Since its inception in 1992, the Li-ion battery has found wide use in portable cellphones and laptop computers, with a typical capacity of 2 Ah (Nemoto, 2003). Larger capacity cells (100 Ah) have been developed for electrical energy storage and electric vehicle markets. In energy storage applications, the energy density is of great importance to minimize the space required for installation (Nemoto, 2003).

Energy generation systems such as gas turbines and diesel engines make use of advanced ceramic coatings as thermal barriers to improve operating efficiencies, service intervals and lifetimes, and fuel economy. These coatings are applied extensively on turbine parts to coat transition pieces, combustion liners, first stage blades and vanes, and in aerospace applications (Riedel, Ionescu, and Chen, 2008). Zirconia-based materials are used most frequently because they fulfil the basic requirements such as high melting point, no phase transformation from room temperature to the operating temperature, thermal expansion match with the substrate, good adherence to the substrate, low sintering rate of the porous microstructure, low thermal conductivity, and chemical inertness (Riedel, Ionescu, and Chen, 2008). However, these materials have a limited operating temperature for long-term applications (< 1473 K), above which sintering occurs and phase transformation leads to cracking (Riedel, Ionescu, and Chen, 2008). Thus there is a need to develop alternative ceramic materials for these applications.

Environmental- and energy-related applications

The oil crisis of the 1970s resulted in a sudden focus on the development of advanced energy-efficient engines with good fuel economy (Pompe, 1994). However these engines had to operate at higher temperatures than conventional engines. This led to the development of high-temperature ceramics capable of handling high temperatures without deterioration in their properties. The main material candidates are based on silicon carbide, silicon nitride, and partially stabilized zirconia (PSZ). In recent years, owing to the drop in energy prices and improved automotive technology, the demand for such ceramic components is greater in power generation applications (energy sector) such as stationary gas turbine parts (Pompe, 1994).

Advanced ceramics have a strong position in the area of environmental restoration, often coupled with energy-related issues. This type of application is primarily legislation-driven but can easily create the market pull to sustain stable growth once established (Pompe, 1994). The major application of advanced ceramics in the environmental industry is in hot gas filtration elements capable of operating at high temperatures (Greil, 2002). Such filters are used as catalyst support due to their high surface area, good mechanical properties and thermal shock resistance, and most importantly the ability to maintain such properties at elevated temperatures. A typical application is in catalytic converters, which are extensively used to remove NOₓ during high-temperature combustion of liquid or gaseous fuels.

The use of ceramic-type heat exchangers (Figure 5) capable of operating at temperatures of up to 1500°C in waste recovery applications has resulted in fuel savings of up to 50%, compared to 20–30% for metallic alternatives (Pompe, 1994). Silicon carbide has been used extensively for this application due to its high wear resistance, corrosion resistance, erosion resistance, and high thermal conductivity.

Advanced ceramics also find use in photovoltaic modules which are used to harness the sun’s energy, and ceramic bearings used in wind turbine generators to improve performance and durability. Ceramic insulators are key components of the fusion power plants (nuclear energy) envisioned in the future, and newer and lightweight materials are increasingly being used in wind turbine construction. Specially designed piezoelectric ceramics in the form of fibre composites are used in energy harvesting (EH) systems to reduce or eliminate the need for battery power, especially in remote locations, by utilizing energy from human and environmental sources (Cass et al., 2008). These devices have been utilized to power wireless sensors employing low-level environmental vibrations as a source of power, to recover waste energy from mechanical forces such as motion, vibration, compression, and tension (Cass et al., 2008). The voltage produced can be converted and stored in capacitors to continuously power electronic devices. To achieve this, a multi-disciplinary approach is required to leverage knowledge in several engineering fields (Cass et al., 2008).

Electronic applications

Owing to their unique properties, advanced ceramics can combine electrical insulation and magnetism, which is not achievable with metals (Riedel, Ionescu, and Chen, 2008). This makes them attractive for electrical applications as they can operate at high power and high frequencies, extreme temperatures, and under harsh environments (Riedel, Ionescu, and Chen, 2008). Advanced ceramics in current use in such applications include silicon carbide in semiconductor devices used in power electronics, barium titanate (BaTiO₃), zirconia titanate (ZrTiO₄), and other ceramics with simple and complex perovskite structures, which are used extensively in microwave applications due to their low dielectric loss and high permittivity (Riedel, Ionescu, and Chen, 2008).

Piezoceramics are widely used for electromechanical sensors and actuators (Riedel, Ionescu, and Chen, 2008). Their working principle is based on electrical and mechanical

Figure 5—A diagram showing sintered SiC heat exchanger tubes for reliable high-temperature high-pressure chemical processing applications (source: Saint-Gobain)
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responses. For instance, when a mechanical force is applied an electrical response in the form of a voltage arises, the magnitude of which is normally proportional to the applied stress. Piezoceramics find extensive use in a wide range of applications which include automotive engineering for gas ignition, ultrasonic parking transducers (Riedel, Ionescu, and Chen, 2008). Also, due to their ability to generate great ultrasonic intensities they are used as ultrasonic transmitters and receivers in signal and information processing applications (Riedel, Ionescu, and Chen, 2008).

Magnetic materials find extensive use in a number of modern technological applications such as in air-conditioning, mobile phones, washing machines, loudspeakers, and electric motors (Abraham and Gupta, 2014). The magnets are required to have certain properties including high strength, corrosion resistance, and resistance to demagnetization due to excessive heat (Abraham and Gupta, 2014). Ceramic magnets were introduced in the 1950s and are the most popular permanent magnets today due to their low cost and the above mentioned properties (Abraham and Gupta, 2014). Non-conducting ceramic ferrites that are biased by a permanent magnet are used extensively for maintaining signal directionality in cellular base stations as isolators and circulators (Figure 7) (Abraham and Gupta, 2014; Hill, 2015). These devices direct radio frequency (RF) signals, at the same time preventing RF power from leaking into unwanted areas (Abraham and Gupta, 2014; Hill, 2015). The requirements for materials for such applications are quite stringent, i.e. the material has to be insulating, with low dielectric losses. This rules out metal magnetic materials and favours the oxide (ceramic) ferrites for such applications (Abraham and Gupta, 2014; Hill, 2015). The most widely used materials for such applications is yttrium iron garnet (Y\textsubscript{3}Fe\textsubscript{5}O\textsubscript{12}).

Electromechanical devices constitute the largest market for permanent magnets and ceramic ferrites, and ceramic ferrites have the largest market share, estimated to have reached a usage of 900 000 t (valued at US$4.5 billion) in 2013. This is projected to grow to 1.1 Mt (valued at US$6.6 billion) in 2018.

In the last few decades, the rapid development of modern communication devices such as cellular telephones, antennae, and global positioning systems has energized research in microwave dielectric materials (Filipovic, 2017). Dielectric ceramics are used widely in advanced electronic devices such as capacitors and microwave resonators. They are classified into two broad groups based on their dielectric properties. High quality factor materials are characterized by linear changes in polarization with applied electric field. This group is dominated by titanate-based materials, with typical examples such as TiO\textsubscript{2}, MgTiO\textsubscript{3}, CaTiO\textsubscript{3}, and SrTiO\textsubscript{3} (Sakabe, 2003). This group is also characterized by a dielectric constant \(k\) of less than 1000 (Sakabe, 2003). The second group is characterized by materials possessing a dielectric constant \(k\) higher than 1000 (Sakabe, 2003). Typical examples include BaTiO\textsubscript{3}-based and lead-based dielectrics.

Thermoelectric (TE) ceramic materials can directly convert heat energy to electrical energy due to thermoelectric effects (Zhang and Zhao, 2015). The majority of thermoelectric devices operating near room temperature are based on bismuth telluride (Bi\textsubscript{2}Te\textsubscript{3}) and its alloys (Zhang and Zhao, 2015).

This group constitute the largest market share of advanced ceramics and its growth is unmatched owing to the continuing advancement of the electronics sector.

Health

Advanced ceramics are increasingly used for prostheses due to a combination of excellent properties which include biocompatibility, wear resistance, stability, and strength (Riedel, Ionescu, and Chen, 2008). In 1970, a French orthopaedic surgeon successfully replaced the conventional stainless steel head of a hip joint with a high-density, high-purity sintered alumina joint (Kokubo, Kim, and Kawashita, 2003). The sintered alumina proved to possess superior properties such as mechanical strength, hardness, chemical durability, and hydrophilicity, and thus could offer a longer service life than conventional stainless steel orthopaedic devices. In February 2003, the US Food and Drug Administration approved the first ceramic-on-ceramic hip joint replacements. Since then, ceramic materials have been utilized in the manufacture of orthopaedic devices for hip

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**Figure 6** — A schematic representation of the piezoelectric and reverse piezoelectric effect, showing areas where piezoceramics find use (Honda Electronics Ltd)

**Figure 7** — A schematic representation of a cellular base station showing the isolators and circulators where ceramic ferrites are used extensively (Hill, 2015)
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joint replacements and dental procedures, with alumina ($\text{Al}_2\text{O}_3$)- and zirconia ($\text{ZrO}_2$)-based ceramics topping the list (Kokubo, Kim, and Kawashita, 2003). The major drawback, however, is the lower fracture toughness values (approx. 2 MPa.m$^{0.5}$) compared to bone (approx. 6 MPa.m$^{0.5}$), which limits the applications of advanced ceramic materials to non-loaded bone replacements. This has prompted the development of reinforced ceramic matrix composites to obtain significantly superior fracture properties in comparison to monolithic ceramics (Kokubo, Kim, and Kawashita, 2003). Over the last few decades, it has been proven that ceramic materials can promote the regeneration of neighbouring tissue, can spontaneously bond to living tissues, and can locally destroy cancer cells to allow normal tissue regeneration after treatment (Charreyron, 2013).

Zirconia is used extensively for dental restoration and was developed into a US$700 million market within a period of less than 15 years (Charreyron, 2013). Zirconia offers significant benefits which include greater durability and the use of digital dentistry that saves time compared to veneer on precious metal castings technology. There are emerging technologies in other medical areas that are likely to create significant new markets for ceramic components, such as implantable devices used to regulate hearts and improve hearing (Charreyron, 2013). These ceramic implantables enable the use of wireless technologies to monitor the devices over the internet (Charreyron, 2013).

Outlook

The industrial revolution witnessed great advances in metal and alloy manufacture and has technically come of age, and it is believed that the 21st century (sometimes referred to as the ‘dot-com age’) will depend on high-performance advanced ceramic materials. There is no doubt that advanced ceramic materials offer superior performance, are long-lasting, and perform crucial functions in a wide range of technological applications, and cannot be easily replicated by other engineering materials. Advanced materials almost always face high market-entry barriers, due mainly to the reluctance of designers and engineers to change from materials they are more familiar with, coupled with unfamiliarity to potential users, redesign requirements, and high cost of components.

The past few decades have seen the emergence of newer technologies which demand more advanced and higher performance engineering materials in a wide range of applications. Advanced ceramic materials have surpassed most traditional engineering materials and remain uncontested in a wide range of such applications. The phenomenal growth experienced is testimony to this. This has been driven mainly by the Asian markets, especially China and Japan. The African continent has yet to take part in the technological boom. Most African states still remain end-user markets for most of these technologies with little or no participation in the research and development of home-grown solutions to technologies requiring advanced ceramics. Infrastructural development programmes, coupled with economic growth on the African continent, are expected to drive new demand for advanced ceramics. The possibility of producing low-cost, mass-produced and high-profit-margin advanced ceramic products is, however, of major concern. The development efforts that have driven the initial boom are crucial for the sustainability of a profitable venture. There are already a number of private entities in the business that possess the technical capability to drive these technologies forward. Africa as a whole needs substantial investment in the development of home-grown solutions requiring advanced ceramics by partnering with institutions that already have the requisite knowledge.

References


Figure 8—Alumina ceramic-on-ceramic component used for hip joint replacements (Kyocera Corp.)
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Effect of dip on pillar strength

by K.V. Jessu and A.J.S. Spearing

Pillar stability is essential for the efficient working of underground mining activities. Unless designed as yield pillars, under-designing can result in failure of a pillar, and furthermore the failure of a pillar may trigger a domino effect, leading to instability of the whole excavation. Conversely, overdesigned pillars can result in sterilization of ore and may be uneconomical to mine.

Normal loading on square and rectangular pillars

Studies have been conducted on pillar size, shape, and stability under normal loading conditions for many years. Hedley and Grant (1972) studied pillar stability in hard-rock (granite) room-and-pillar mines and derived an empirical relationship between the strength of the pillars and their width to height ratio. Lunder and Pakalnis (1998) considered the role of confinement in determining the strength of hard-rock pillars. Martin and Maybee (2000) led numerical modelling studies using elastic-brittle-plastic theory to determine the pillar strength and concluded that dip pillars have comparatively less strength than horizontal pillars. Esterhuizen (2008) concluded that for room-and-pillar workings in limestone, slender pillars have variable strength depending on the geological structures, while there is little effect on squat pillars.

Rectangular pillars have proven to be effective in maximizing pillar strength, as suggested by several researchers (Wagner, 1992; Mark and Chase, 1997; Galvin, Hebblewhite and Salamon, 1999; Dolinar and Esterhuizen, 2007). Geological structures have less impact on rectangular pillars when oriented with longer axis of the pillar (Dolinar and Esterhuizen, 2007). Little research has been undertaken to date on inclined rectangular pillars.

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Effect of dip on pillar strength

Pillars dipping at an angle are subjected to oblique loading which is a combination of the compressive and shear stresses as shown in Figure 1. Pritchard and Hedley (1993) classified the pillars at Denison Mine into five categories, based on progressive failure. This investigation was conducted in an orebody dipping at 20° with the pillars showing the hourglass fracture pattern which ultimately leads to the critical cross-section area of the pillar core and ultimate failure.

Suorineni et al., (2011) noted that both pillars and excavations under oblique loading are at elevated risk of failure. This was supported by the case studies reported by Kvapil, Beaza, and Flores (1989), Hedley, Roxburgh, and Muppalaneni (1984), and Whyatt and Varley (2008). Suorineni. (2014) conducted studies to determine the failure modes of pillars with different orientations of far field principal stresses with the help of two-dimensional numerical modelling, and concluded that the most significant factors are the orientation of the far field principal stresses, width-to-height ratio, and maximum to minimum principal stress ratio.

In this paper we examine the failure modes of pillars at different pillar inclinations and determine the strength of inclined square the rectangular pillars at different width-to-height ratios.

Modelling approach

Numerical modelling provides valuable insight into the potential failure modes of the pillar if the input parameters are carefully selected and calibration is possible. FLAC3D (Itasca, 2016), a finite difference numerical modelling package, was used to simulate the horizontal and inclined pillars as the models can be calibrated to the realistic failure methodology of hard-rock pillars (Esterhuizen, 2006). A three-dimensional model was constructed to study individual horizontal and inclined pillars as shown in Figure 2.

The coordinate system used is represented by x and y axes in the horizontal plane and the z axis in the vertical plane. The main roof, pillar, and main floor are the major components of the model, as shown in Figure 2. The height of the pillar is kept constant and the width is varied to simulate the different width-to-height ratios. The extraction ratio of the horizontal pillar model was kept constant at 75% and the boundaries of the inclined pillars were far enough to avoid its influence the pillar system behaviour.

The floor was fixed such that the displacements and the velocities are restricted in the normal and parallel directions. Side boundaries were incorporated with rollers such that the displacements and the velocities were restricted only in the normal direction. A uniform velocity was applied on top of the main roof to simulate the compressional loading in the horizontal model and oblique loading in the inclined pillar model (Lorig and Cabrera, 2013).
Effect of dip on pillar strength

To simulate a pillar at a depth of 300 m, the pillar was subjected to a vertical stress of about 8.1 MPa and the vertical-to-horizontal stress ratio was maintained at 1:1. The model was run to equilibrium under elastic conditions and was converted to bilinear conditions after reaching equilibrium, representing the rock mass behaviour as elastic before the excavation and as brittle and plastic after the excavation (Esterhuizen, 2006). The pillar models were then subjected to uniform velocity until complete failure. The stress-strain curves were developed to obtain the strength of the pillars with the help of a FISH function (FISH is script in FLAC3D to derive user-defined variable and functions).

The properties are critical in numerical modelling to ensure the model is realistic. The performance of the pillars is best represented by the brittle Hoek-Brown criterion (Martin, Kaiser, and McCreath, 1999; Kaiser et al., 2000; Esterhuizen, 2006). The brittle Hoek-Brown damage initiation criterion is established on the development of the brittle cracks, which generally occurs at 0.3 to 0.5 times the uniaxial compressive strength (UCS) and is followed by shear failure of the pillar. The UCS of the rock was taken to be 150 MPa, therefore the rock mass compressive strength was estimated to be about 50 MPa. Therefore, a bilinear strength envelope was used in which strength is independent of friction at low confinement and is equal to one-third of the UCS, followed by friction-hardening at the higher confinement (Kaiser, 2000).

The sub ubiquitous model, an inbuilt FLAC3D program is designed to simulate the bilinear rock strength based on Mohr-Coulomb failure criterion and strain-softening as a function of deviatoric plastic strain (Itasca, 2016). The rock properties and joint properties of the model were derived with the help of the UCS and the rock mass rating (RMR) of 70, which were obtained by Dolinar and Esterhuizen (2007). The model properties are shown in the Tables I and II.

Strain-softening performance is dependent on the model element size, which is determined by using the same element size throughout all the models and calibrating the numerical model results to the theoretical results (Itasca, 2016). The model element size was kept at 0.5 m x 0.5 m x 0.5 m for all the models and cohesion softening was performed to calibrate the model results with the Lunder and Pakalnis (1994) results.

Model calibration

The models were generated at width-to-height ratios of 0.5, 1.0, 1.5, and 2.0. The modelled strength of the pillars was then compared to the theoretical results from Lunder and Pakalnis (1994) as shown in Figure 3. The difference between the model results and the theoretical results is less than 2%.

Results and discussion

Effects on strength of inclined pillar under oblique loading

The inclined pillars experience oblique loading, which is a
combination of compressive load and shear load. To evaluate the strength of the inclined pillars, the models were simulated with pillar inclinations of 0°, 10°, 20°, 30°, and 40° at varying width-to-height ratios. The results are shown in Figure 4. The pillar strength decreases with increasing pillar inclination, similar to the results presented by Suroineni et al. (2014). At lower W/H ratios, the pillar strength decreases slightly with increasing pillar inclination. At higher W/H ratios, as the pillar inclination increases, the strength of the pillar is drastically reduced compared to the pillar under pure compression.

**Failure modes of inclined pillars**

The pillar failure mechanism was studied to understand the load shedding after failure of the pillars. Failure modes were derived from the model results. To understand the pillar failure modes, a section at the centre of the pillar in the direction of the y-axis (Figure 5) has been extracted and the yielded zones presented for all the pillar inclinations at different stages of loading. The failure modes of the horizontal and the inclined pillars will be presented for width-to-height ratios of 0.5 and 1.0. The horizontal pillars undergo only compressive loading while the inclined pillars undergo oblique loading, which is a combination of compressive and shear loads.

**Pillar failure modes at W/H 0.5**

Stress-strain graphs of pillars with a W/H ratio of 0.5 at different inclinations are presented in Figure 6. At a W/H ratio of 0.5, the pillars fail by brittle spalling. This is mainly due to the absence of sufficient confinement in the pillars to mobilize the frictional component of the rock strength (Esterhuizen, 2006). The progressive failure of the pillars has been shown in five stages, which are indicated in Figure 6. The progressive failures of the horizontal and inclined pillars are shown in Figure 7. Stage 1 represents the pillar before loading. The elastic zones are highlighted in blue and the yielded zones in green.

Horizontal pillars (0° pillar) fail as the average pillar stress approaches the brittle rock strength. The brittle rock strength (50 MPa), which is one-third of the UCS (150 MPa), can be observed in the graph as the strength of the horizontal
Effect of dip on pillar strength

Figure 6—Stress-strain curves for pillars with a W/H ratio of 0.5 at different inclinations

Figure 7—Failure modes of pillars with W/H ratio of 0.5 at different stages. (a) Horizontal pillar, (b) 10° inclination, (c) 20° inclination, (d) 30° inclination, (e) 40° inclination. (Note: blue zones represent elastic behaviour and green zones represent yielded/plastic behaviour)
Effect of dip on pillar strength

pillar. The brittle failure commences at the outer skin of the pillar and progresses towards the core at the peak stress. At stage 2, where the pillar is at its peak stress, the core has yielded, leading to total failure of the pillar as shown in Figure 7a. At stage 3, the post-peak stage of the pillar stress, the pillar has totally yielded (Esterhuizen, 2006). The final stages (stages 4 and 5) are represented as totally yielded, meaning the whole pillar is yielded (all zones are green).

For inclined pillars, stage 1 shows the pillar before loading (Figure 7). Stage 2 shows the initiation of yielding on the outer skin of the pillars in the opposite corners in the direction of the pillar inclination. Stage 2 at 10° pillar inclination shows the yielding extending towards the centre of the pillar, similar to the formation of a shear plane (Figure 7b). As yielding occurs at the centre of the pillar before total yielding of the outer skin, the strength of the pillar is reduced compared to that of the horizontal pillars.

Stage 3 of the inclined pillars shows that at lower pillar inclinations, yielding extends from the outer skin of the pillar towards the centre (Figures 7b and 7c). At higher pillar inclinations, yielding is developed only at the two corners of the pillars in the direction of pillar inclination (Figures 7d and 7e). It can be concluded that the higher the inclination of the pillar, the higher the axial strain it can sustain to attain the peak strength, which may be due to sliding at the roof and floor contacts. It can also be observed that with increasing pillar inclination, the yielding distance between the two corners of the pillar decreases, contributing to the strength reduction.

Stage 4 of the pillars at lower inclination (10° and 20° dip) shows the post-peak phase of the pillar (Figures 7b and 7c), while for the 30° pillar the yielding has extended from the outer skin to the centre of the pillar, as shown in Figure 7d. It can be observed that the 30° pillar at stage 4 is at its peak strength when the yield is approaching the centre of the pillar (Figure 6). Therefore, it can be concluded that the reduction in pillar strength with increasing inclination is a result of the loss of the core. Stage 4 of the 40° pillar shows yielding at the outer skin of the pillar (Figure 7e).

At stage 5, the pillars at inclinations less than 40° are in the post-peak phase (Figures 7b, 7c, and 7d). The yielding in the 40° pillar at stage 5 extends towards the centre, which is the point of peak strength in the pillar (Figure 7e). It can be observed that for the inclined pillars there is a rapid increase in yielding in the last stage. For example, the yielding in stage 4 of the 40° pillar is limited to the outer skin, while in stage 5 the yielding has reached the centre of the pillar.

To summarize, failure along a single plane in a brittle fashion is the dominant failure mechanism for pillars at all inclinations with W/H ratio of 0.5. Failure commences at the two pillar corners in the direction of pillar inclination and extends towards the centre of the pillar, which results in the peak strength of the pillar being reached. At higher inclinations, the pillar can sustain higher axial strains while the pillar strength is reduced. It is well-known that, for a horizontal pillar, the yielding of the outer skin is an indication that the pillar is at its peak strength (Esterhuizen, 2006); but for inclined pillars the yielding at the outer skin may not justify that conclusion.

Since the pillar core is yielded even before the complete yielding of the outer skin, it can be concluded that the failure can be violent, such as in a pillar burst. The rapid increase in the yielding of the inclined pillars from the outer skin towards the centre of the pillar is also a factor contributing towards the violent failure of inclined pillars.

Pillar failure modes at W/H 1.0

Stress-strain graphs of pillars with a W/H ratio of 1.0 at different inclinations are shown in Figure 8. The horizontal pillar fails by brittle failure at the outer skin of the pillar, followed by shear failure. The transition from brittle failure to shear failure is also observed in Figure 8, where the slope of the stress-strain curve for the horizontal pillar changes. The progressive failure of the pillars is shown in four stages, as in Figure 8. The horizontal and inclined pillar with zero initial load are shown in stage 1 of Figure 9.

In stage 1, elastic zones extend throughout the pillars as they are at zero load. Stage 2 of the horizontal (0°) pillar shows that yielding commences at the four corners of the pillar by brittle failure (Figure 9a). As the pillar load increases, the extent of brittle failure increases, followed by
Effect of dip on pillar strength

shear failure. Stage 3 and stage 4 show complete brittle failure at the outer skin of the pillar and shear failure starting to develop behind the brittle failure zones. The pillar stress can decrease before the core of the pillar has yielded by shear failure, similar to the results of the compression tests on small coal pillars reported by Wagner (1974).

Stage 1 of the inclined pillars shows the pillars before loading (Figure 9). Stage 2 shows that the stress on the pillar is accumulated at the two opposite corners of the pillar in the direction of inclination, due to the brittle failure (Figure 9).

Stage 3 for the 10° pillar shows complete yielding of the outer skin of the pillar due to brittle failure in the shape of an ‘inclined hourglass’ (Figure 9b), while for the 20° pillar, the brittle failure extends from the two corners of the pillar to the interior (Figure 9c). For pillars at 30° and 40° inclination, the brittle failure extends from the outer skin towards the centre of the pillar (stage 3 in Figures 9c and 9d). Therefore, the pillars at higher inclinations can sustain more axial strain to attain the peak strength, which may be due to sliding of the pillars at the roof and floor contacts.

<table>
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<tr>
<th>Dip °</th>
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Figure 9—Failure modes of pillars with W/H ratio of 1.0 at different stages. (a) Horizontal pillar, (b) 10° inclination, (c) 20° inclination, (d) 30° inclination, (e) 40° inclination. (Note: blue zones represent elastic behaviour and green zones represent yielded/plastic behaviour)
The extent of brittle failure in the pillars inclined at 10° and 20° is greater than in the horizontal pillar, as seen in stage 5 of Figure 9b and stage 4 of Figure 9c, which show the ‘inclined slender hourglass’ pattern with increasing in pillar inclination. Due to brittle failure, the depth of yielded zones has also increased in the inclined pillars. As the load increases, shear failure commences behind the brittle failure, and the outer skin of the pillar is completely transformed into a brittle failure zone. The area of shear failure decreases with increasing inclination, leading to lower strength of the inclined pillars.

At higher pillar inclinations of 30° and 40°, brittle failure extends towards the centre of the pillar, leading to core loss (stage 4 of Figures 9c and 9d). This occurs at the peak stress of the pillar. Due to this loss of confinement in the inclined pillars, shear failure is absent in the pillars inclined at 30° and 40°, leading to lower strength of these pillars. It can also be observed that the yielding at stage 4 of 30° and 40° pillars increases rapidly, which indicates that the pillars at a W/H ratio of 1.0 can also undergo violent outbursts at higher inclinations.

In summary, pillars with a W/H ratio of 1.0 at lower inclinations develop the ‘inclined hourglass’ pattern, which becomes slender with increasing inclination. At higher inclinations, the dominant failure mode is brittle failure which extends towards the centre of the pillar, leading to a violent outburst as the core yields even before the outer skin of the pillar. The rapid yielding of the higher inclination pillars due to brittle failure may also be a basis for violent outbursts.

The pillar failure mechanism of 20° inclined models is similar to the behaviour observed in the Dension Mine (Pritchard and Hedley, 1993). Minor spalling at the ends of the pillars can be observed in the models (stage 3 of Figure 9c) as well as in Figure 10. This then progresses to strong sidewall spalling, which resembles stage 4 of Figure 9d. Therefore, it can be concluded that pillars at other inclinations would show a similar failure mechanism in situ, as shown in Figures 9 and 11.

**Failure mechanism in pillars with W/H ratio greater than 1**

The failure mode changed from complete brittle failure at a W/H ratio of 0.5 to partial brittle failure plus shear failure at a W/H ratio of 1.0. This transformation was observed for 0°, 10°, and 20° pillars, while the 30° and 40° inclined pillars were observed to undergo total brittle failure. A similar transformation will be observed at higher inclinations with an increase in W/H ratio. Therefore, it can be concluded that the failure mode of the pillars will be partial brittle failure in combination with shear failure, as indicated in Figure 11.

The ‘inclined hourglass’ pattern in the inclined pillars, which becomes slender with increasing inclination, can be attributed to the decrease in the strength of the inclined pillars at a W/H ratio of 1.0. It can be concluded that similar behaviour will be observed at higher W/H ratios.

**Rectangular pillars**

Models were simulated to investigate the effects of inclination on rectangular pillars. The inclined rectangular pillars can be classified into dip and strike pillars. For the strike pillars, the length of the pillar was increased perpendicular to the direction of inclination (Figure 12a), while for the dip pillars, the length was increased parallel to the inclination (Figure 12b). The three width to height ratios of 0.5, 1.0, and 1.5 and length to width ratios of 1.0, 2.0, and 3.0 at varying pillar inclinations were simulated.

For the horizontal pillars, the rectangularity in dip is equal to the strike. The pillars with W/H ratios of 0.5 and different L/W ratios have similar strengths as the failure mechanism is dominated by brittle failure. At W/H ratios of 1.0 and 1.5, the pillar strength increases with increasing L/W ratio, as shown in Figure 13. This is due to the fact that at higher W/H ratios, shear is the dominant failure mechanism and with the increase in length, a large shear failure area is formed which results in an increase in pillar strength. It can be concluded that pillar strength increases with increasing L/W ratio at higher W/H ratios. The model results are similar to the results of Dolinar and Esterhuizen (2007).
Effect of dip on pillar strength

<table>
<thead>
<tr>
<th>Dip</th>
<th>W/H ratio 0.5</th>
<th>W/H ratio 1.0</th>
<th>W/H ratio 2.0</th>
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<td><img src="image3" alt="Diagram" /></td>
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<td><img src="image13" alt="Diagram" /></td>
<td><img src="image14" alt="Diagram" /></td>
<td><img src="image15" alt="Diagram" /></td>
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</tbody>
</table>

Figure 11—Pillar failure classification with brittle and shear failure at different inclinations

Figure 12—Model of 10° inclined rectangular pillar with W/H ratio of 0.5 and L/W ratio of 2.0 along (a) strike and (b) dip
Effect of dip on pillar strength

The rectangular pillars at 10°, 20°, 30°, and 40° inclinations along the strike and dip were simulated to determine the strength. Fifteen models each for different inclinations were modelled: three with W/H ratios of 0.5, 1.0, and 1.5 and L/W ratio of 1.0 to represent the square pillars, and six models each with W/H ratios of 0.5, 1.0, and 1.5 and L/W ratios of 2.0 and 3.0 along both strike and dip.

At 10° inclination, the strength of the pillar increases with increasing pillar length at higher W/H ratios, but remains the same at a W/H ratio of 0.5. The pillars with lower W/H ratios show no difference in strength between the dip and strike pillars (Figure 14), similar to the horizontal pillars (Figure 13). At a higher W/H ratio of 1.5, the dip pillars have a 7% higher strength than the strike pillars, as shown in Figure 14.

At inclinations of 20° and 30°, the strength of the pillars increases with increasing length along dip at any W/H ratio.

Inclined rectangular pillars

The rectangular pillars at 10°, 20°, 30°, and 40° inclinations along the strike and dip were simulated to determine the strength. Fifteen models each for different inclinations were modelled: three with W/H ratios of 0.5, 1.0, and 1.5 and L/W ratio of 1.0 to represent the square pillars, and six models each with W/H ratios of 0.5, 1.0, and 1.5 and L/W ratios of 2.0 and 3.0 along both strike and dip.

At 10° inclination, the strength of the pillar increases with increasing pillar length at higher W/H ratios, but remains the same at a W/H ratio of 0.5. The pillars with lower W/H ratios show no difference in strength between the dip and strike pillars (Figure 14), similar to the horizontal pillars (Figure 13). At a higher W/H ratio of 1.5, the dip pillars have a 7% higher strength than the strike pillars, as shown in Figure 14.

At inclinations of 20° and 30°, the strength of the pillars increases with increasing length along dip at any W/H ratio.
Effect of dip on pillar strength

Figure 15—Strength results for 20° inclined rectangular pillars with lengths extended along dip and strike

Figure 16—Strength results for 30° inclined rectangular pillars with lengths extended along dip and strike

Figure 17—Strength results for 40° inclined rectangular pillars with length extended along dip and strike
Effect of dip on pillar strength

It can also be observed that the rectangular pillars with length extended along strike show minimal to no increase in strength compared to square pillars (Figure 15 and 16). At a W/H ratio of 1.5, the strengths of the rectangular dip pillars are 14% and 20% greater than the corresponding rectangular strike pillars, respectively. Therefore, it can be concluded that rectangular pillars are beneficial only when the longer axis is along dip and the W/H ratio greater than or equal to 1.5. At 40° inclination, it can be observed that the strike pillars show no increase in strength with an extension of length at any W/H ratio (Figure 17). This means that the square pillar with W/H ratio of 1.5 and the rectangular pillar with W/H ratio of 1.5 and L/W ratio of 5 along the strike have equal strengths. This fact can be used for determining the strength of the sill pillars. Sill pillars are inclined and considered to be infinite in length and are modelled in a two-dimensional framework. A simple approximation for the strength of the sill pillars can be made by calculating the strength of a square pillar at that inclination and W/H ratio. This holds true as the length is not contributing towards the increase in pillar strength.

Conclusions

Based on the modelling undertaken, the effects of dip on pillar strength can be summarized as follows:

► The pillar strength is relatively low at higher inclinations compared to horizontal pillars. Therefore, higher safety factors are required while designing inclined pillars with the traditional approaches.

► Brittle failure is the dominant failure mechanism in the inclined pillars. The initiation of the pillar deformation is sluggish, but the pillar yields rapidly when it reaches its peak stress.

► Violent outbursts can be expected in the inclined pillars due to core loss, even before the deformation in the outer skin of the pillar, and also due to the rapid deformation near the peak pillar stress.

► Rectangular pillars are beneficial only with W/H ratios greater than 1.5 and when the length of the pillars is increased along the dip at higher inclinations.

► The strength of the sill pillars can be approximated using the results for square pillars, as inclined pillars with increased length in the strike direction show minimal to no increase in strength compared to square pillar.

References


The Lily Mine collapse in 2015, caused by the failure of a crown pillar, highlighted the need to pay more attention to the effects of underground mining on the surface in South Africa. This topic was of great interest in coal mining in the 1990s, with the increased prevalence of high-extraction coal mining methods that invariably resulted in surface subsidence. The subsidence and methods for predicting it were described by several authors, such as Wagner and Schümann (1991), van der Merwe (1991), and Canbulat et al., (2002). However, since that time, the matter had been regarded as being under control and very little further development took place.

Shallow underground coal mining (i.e., mining depth from 20 m to 200 m), such as practiced in South Africa, can potentially result in two different types of surface disturbance. The first is trough-like subsidence characterized by relatively wide areas with limited amounts of subsidence and gently sloping sides, and the second is pit-like subsidence, characterized by relatively small areas, greater depths of subsidence, and approximately vertical sides. Each of those can be further subdivided, as will be described in this paper.

The paper provides updates and further developments based on previously published material, incorporating new data and better insights. This can then be used for mine planning and for the evaluation of long-term stability of the surface overlying coal workings.

Most of the results presented in the paper were derived empirically, or were at least based on empirical observations followed by further analyses. It is thus important to be aware of the limits of applicability – depth of mining 20 m to 200 m, mining height 1.5 m to 6 m, bord width 5 m to 7.5 m, and panel width 80 m to 300 m.

### Trough-like subsidence

The situations that can potentially result in trough-like subsidence are pillar failure and high-extraction mining methods like short- or longwalling or pillar extraction. This style of subsidence is characterized by relatively large areas of subsidence (approximately the same size as the underground mined panels) and limited depths of subsidence, less than the mining height (see Figure 1 for examples).

The majority of subsidence profiles can be classified as subcritical, meaning that the profiles are not characterized by a flat portion in the centre. According to Wagner and Schümann (1991) the critical panel width for supercritical profiles (i.e., with a flat portion in
the centre) is between 1.7 and 3 times the depth of mining, while van der Merwe (1991) quotes a figure of 2.5.

There are distinct differences between the trough-like subsidence caused by high-extraction methods and that caused by pillar failure.

**Subsidence caused by high-extraction mining**

High-extraction mining typically results in smooth subsidence profiles with the vertical displacement less than the mining height.

**Vertical subsidence**

In the case of longwall mining in the absence of a strong layer such as dolerite in the overburden, the maximum amount of subsidence, \( S_m \), according to van der Merwe (1991), is a function of the depth of mining, panel width, and mining height and is given by

\[
S_m = 0.39h \left( \frac{W}{H} \right)^{0.32} \text{ (m)}
\]

where \( h = \) mining height (m), \( H = \) mining depth (m), \( W = \) panel width (m).

Subsequent routine observations by the author at several sites have indicated that for pillar extraction, Equation [1] should be modified by the incorporation of a modifying factor, \( f_m \):

\[
f_m = 0.85e
\]

where \( e \) is the areal extraction ratio, typically 0.75 or less for pillar extraction.

The full expression for the maximum subsidence is then

\[
S_m = 0.39f_m h \left( \frac{W}{H} \right)^{0.32} \text{ (m)}
\]

where \( f_m = 1 \) for longwall mining.

The ‘severity’ of the profile is classified according to the \( S_m/H \) ratio, as given by van der Merwe and Madden (2010) and reproduced in Table I.

In the case of multiple seam mining, the subsidence caused by extraction of the first seam is given by Equation [3] while the additional subsidence caused by extraction of the subsequent seams is approximately equal to the full effective mining height of the additional seams (van der Merwe, 1986).

**Shape of subsidence trough**

According to van der Merwe (1991), the shape of the half-profile of the subsidence trough is described by

\[
S_x = 0.5s_m \left( \tanh \frac{7.31x}{W} - 1.44 \right) + 1 \text{ (m)}
\]

where \( S_x \) is the subsidence at a position \( x \) from the solid ribs side measured toward the centre of the panel.

Inspection of the data on which Equation [4] is based indicated considerable variability (see Figure 2). Upper and lower limits were fitted to the family of curves derived by van der Merwe (1991) and these are described by Equations [5] and [6] respectively.

\[
s_{x(lower)} = 0.5s_m \left( \tanh \frac{7.31x}{W} - 1.44 \right) + 1 \text{ (m)}
\]

\[
s_{x(toupper)} = 0.5s_m \left( \tanh \frac{7.73x}{W} - 2.146 \right) + 1 \text{ (m)}
\]

**Table I**

<table>
<thead>
<tr>
<th>Class</th>
<th>( S_m/H )</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>&lt;0.001</td>
<td>Barely noticeable, smooth, continuous profile, hairline cracks</td>
</tr>
<tr>
<td>B</td>
<td>0.001–0.005</td>
<td>Difficult to notice, smooth profile, cracks 1 to 2 cm wide</td>
</tr>
<tr>
<td>C</td>
<td>0.005–0.02</td>
<td>Noticeable in flat terrain, smooth, cracks 2 to 10 cm wide, compression ridges 1 to 5 cm high</td>
</tr>
<tr>
<td>D</td>
<td>0.02–0.05</td>
<td>Noticeable in most terrains, visible vertical displacements across cracks, cracks 10 to 50 cm wide, compression ridges 5 to 50 cm high</td>
</tr>
<tr>
<td>E</td>
<td>&gt;0.05</td>
<td>Severe profile, almost vertical sides, cracks wider than 50 cm, compression ridges higher than 50 cm</td>
</tr>
</tbody>
</table>
It is considered more reasonable to state that subsidence can be expected to be within the limits described by Equations [5] and [6]. These equations can also be used to evaluate the potential for further subsidence in the case where subsidence has already occurred – if the measured profile plots above the upper limit, it should be considered that there is potential for further subsidence in the future.

Another use of the equations is to construct three-dimensional plots of the subsidence, using the following method.

- Calculate maximum subsidence $S_m$, using Equation [3]
- Construct a subsidence half-profile for the length of the panel using either of Equations [4], [5], or [6]
- Construct successive transverse half-profiles, again using either of Equations [4], [5], or [6], but using the relevant $S_x$ in that position for $S_m$ in the equation.

It should also be noted that for single seam workings, meaningful subsidence from a structural point of view is contained within the mined panel delineated by inter-panel pillars, but that small amounts of subsidence do in fact occur over the solid areas as well. Between 1% and 5% of the maximum subsidence can be expected to occur directly over the edges of the underground panel.

**Related elements of subsidence**

Vertical subsidence per se is of major significance only where ponding may result. For all other purposes, the induced tilt and strain are more important. Figure 3 shows a simplified half-profile of a subsidence trough with exaggerated vertical scale, where the induced strain and tilt are also shown.

The point of inflection is important in subsidence engineering. It marks the position where half of the maximum subsidence occurs, which is also the position of maximum tilt and the position where the strain changes from tensile at the edges of the subsidence profile to compressive in the central region. It has been seen (van der Merwe, 1991) that the point of inflection typically occurs at a lateral position $0.23W$ from the solid edge of the underground panel.

The maximum tensile strain – which is where surface cracks can be expected – occurs at a position $0.12W$ from the edge, and the maximum compressive strain – where compression ridges can occur – at a position $0.36W$.

It has been shown (van der Merwe, 1991) that the magnitudes of tilt and strain can be determined based on the half-profiles of subsidence. However, due to the variability inherent in subsidence profile shapes, the real magnitudes are more variable than those predicted from the profiles. The maximum values to be expected, empirically based on measurement according to van der Merwe (1991), are:

- **Tilt:**
  \[ T_m = 21.6S_m + 7 \text{ (mm/m)} \]  
  \[ \text{(7)} \]

- **Compressive strain:**
  \[ \varepsilon_{m-} = -9.1S_m - 2.8 \text{ (mm/m)} \]  
  \[ \text{(8)} \]

- **Tensile strain:**
  \[ \varepsilon_{m+} = 4.2S_m + 1.7 \text{ (mm/m)} \]  
  \[ \text{(9)} \]

where $S_m$ is the maximum subsidence (in metres) obtained by Equation [3].

Note that the magnitude of compressive strain is greater than the tensile strain, which is characteristic of subcritical subsidence profiles.

For certain structures the induced radius of curvature, $R$, is an important parameter. It can be estimated based on geometrical considerations by fitting a circle segment through the edges and centre of a subsidence profile:

\[ R = \frac{W}{4\sin\left(\frac{\sqrt{2S_m}}{W}\right)} \text{ (m)} \]  
\[ \text{(10)} \]

**Subsidence caused by pillar failure**

There are distinct differences between the processes of subsidence caused by high-extraction mining and that caused by pillar failure (van der Merwe and Madden, 2010). The resulting subsidence also differs. There is a scarcity of data describing pillar failure subsidence, implying that the subsidence is predicted with lower confidence than that for high-extraction mining. The scarcity of data is due to the fact that pillar failure is a rare occurrence, with an estimated 0.3% of bord-and-pillar panels having failed in South Africa, based on information in van der Merwe and Matthey (2013). It is also not possible to know beforehand where and when such failures will occur, and consequently accurate elevations of the pre-failure topography can rarely be obtained.
Previously, indicative data by MacCourt, Madden, and Schümann (1986) was used to estimate the magnitude of subsidence that can be expected in the event of pillar failure. Their data has since been supplemented by new cases and the new database was re-examined (see Figure 4). The data is presented in Table II.

**Subsidence due to pillar failure**

It was found that the maximum subsidence is related to the equivalent mining height, \( h_e \) (i.e. the product of physical mining height and extraction ratio) and depth of mining, \( H \), as follows:

\[
S_{m,PF} = \frac{4.66h_e/\mu}{0.068+4h_e/\mu} \text{ (m).} \tag{11}
\]

**Tilt due to pillar failure**

The maximum tilt was also found to relate to \( h_e \) and \( H \) (see Figure 5). The relationship was found to be

\[
T_m = 8036 \left( \frac{h_e}{H} \right)^{1.36} \text{ (mm/m)} \tag{12}
\]

**Effect of a dolerite sill**

Subsidence may be arrested by the presence of a thick, strong layer in the overburden that is capable of bridging across the mined panel. In South Africa, dolerite sills frequently occur in the overburden and have received attention in this regard. Dolerite is known to be vertically jointed, and based on a modified key-block mechanism, van

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**Table II**

Database of subsidence caused by pillar failure. The amounts of subsidence and tilt are estimates

<table>
<thead>
<tr>
<th>Case</th>
<th>Coalfield</th>
<th>Seam</th>
<th>Depth (m)</th>
<th>Pillar width (m)</th>
<th>Bord width (m)</th>
<th>Mining height (m)</th>
<th>Subsidence (m)</th>
<th>Tilt (mm/m)</th>
<th>Extraction (%)</th>
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</thead>
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<td>6</td>
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<td>135</td>
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<td>3.6</td>
<td>2.2</td>
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<td>79</td>
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<td>7.6</td>
<td>6</td>
<td>3.6</td>
<td>577</td>
<td>80</td>
</tr>
</tbody>
</table>
der Merwe (1995) derived the following expression for the minimum span required to result in sill failure:

\[ L = \frac{f}{2} \left[ 2T \sqrt{\frac{2 + \beta}{D}} + 2(H - D)\tan\phi \right] \text{ (m)} \]  

\[ y = 0.025 \frac{D - T}{D} + 0.03 \frac{T}{D} \]  

\[ \beta = \frac{1.53 y}{\gamma} - 0.8 \]  

Equations [13] to [15] can be used to plan panel widths in order to control the state of the sill. The temptation to design for an intact sill in order to prevent subsidence from occurring over the long term should be avoided, as cases have been known where the sill was initially intact but failed several years after mining.

**Sinkhole subsidence**

Two sub-classes of subsidence are discussed under this heading. The first is sinkholes caused by progressive roof collapse from underground, which results in the sudden formation of a sinkhole when the cavity reaches the surface. The second is the occurrence of much smaller sinkholes caused by subsurface erosion, which is a consequence of trough subsidence that only manifests after a period of time ranging from months to decades.

**Progressive roof collapse**

This mechanism is described in Canbulat et al. (2002), who derived an equation for the maximum height a cavity can reach before it is arrested by the increase in bulking from the collapsed material. Their equation, however, is only valid for a sinkhole with diameter equal to the width of bord intersections underground and for cavities with vertical sides. Neither condition is necessarily true.

A simplified cross-section of a cavity choked by rubble from the collapse is shown in Figure 6. It shows the cavity as a truncated cone, as opposed to a cylinder.

For the cavity to be choked by bulked rubble from the roof collapse, the bulked volume \( A \) in Figure 6 must be equal to the sum of volumes \( A \), \( B \), and \( C \). Therefore,

\[ K \text{(Volume } A \text{)} = \text{Volume } A + \text{Volume } B + \text{Volume } C \]  

or

\[ K = 1 + \frac{\text{Volume } B + \text{Volume } C}{\text{Volume } A} \]  

where \( K \) is the bulking factor.

Then,

\[ \text{Volume } A = \frac{1}{2} \left[ \pi f D_B^2 \right] z_m - \frac{\pi D_B^2}{4} (z_m - z) \]  

\[ = \frac{\pi}{12} [(f D_B)^2 z_m - D_B^2 (z_m - z)] \]  

where \( f \) = ratio of bottom of sinkhole diameter to bord width (i.e. for sinkhole diameter equal to intersection width, \( f = 1.414 \))

\( D_B \) = bord width

\( z \) = height of choked volume

\( z_m \) = maximum height of cavity when it wedges out

\[ D_B = \text{diameter of top of choked cavity} \]

\[ B = h D_B^2 \]

where \( h \) = mining height

\[ C = \text{Volume } C = 4 \left( \frac{1}{2} h^2 D_B \cot\alpha \right) = 2h^2 D_B \cot\alpha \]

where \( \alpha \) = angle of repose of collapsed material.

Where the pillars are small, it is possible for the toes of the collapsed rubble to touch, restricting the volume available for the bulked material in the roadways – see Figure 6a. This can happen where the pillar width, \( w \), is less than \( h \cot\alpha \). In that case, Equation [20] should be substituted with

\[ \text{Volume } C = D_B \left( 2wh - \frac{w^2}{2} \tan\alpha \right) \]

Equation [20a] should be used with caution as it is valid only if adjoining intersections collapse at the same time and extend at the same rate. Experience indicates that adjoining sinkholes very seldom form at the same time.

Also,

\[ Z_m = \frac{f D_B}{2 \tan\phi} \]

and

\[ D_B = f D_B - 2 z \tan\phi \]

where \( \phi \) is the caving angle.

Substitution of Equations [18] to [22] into [17] results in a cubic expression for \( z \), which is easiest to solve by

![Figure 6—Simplified cross-section through a cavity caused by roof collapse](image-url)

![Figure 6a—Simplified diagram showing touching toes of rubble](image-url)
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iteration. This is a much more complex equation than the one given in Canbulat et al. (2002), but is relatively easy to solve with commonly available calculation tools like Microsoft Excel®. This procedure allows for two mechanisms of termination of cavity growth, namely wedging out or choking by bulked roof material, and it is more generic as it allows for any inclination of the cavity sidewall or diameter of the cavity.

The maximum height of the cavity is then the smallest of $z$ obtained with Equation [17] (termination by choking) or $z_m$ obtained with Equation [21] (termination by wedging out).

The intuitive method is to adopt the conservative approach by assuming vertical sidewalls and then using the original Canbulat et al., (2002) equation as it may be believed to represent the worst case. However, the cavity will in fact progress higher with inclined sidewalls than with vertical ones, and therefore the conservative approach is to use Equation [17]. The higher the cavity, the smaller the diameter of the hole at its upper end.

If the maximum height of the collapse reaches the bottom of the weathered rock or soil on surface, it should be assumed that a sinkhole will develop. It is then merely a matter of time before the soft materials collapse or are washed into the hole underneath.

This approach can be further refined by considering the stability of the ‘lid’ of the sinkhole in the case where the cavity is completely filled before reaching the bottom of the weathered rock. For this, it is considered that the ‘lid’ is loaded by the rock and soil above. The tensile strain then developed in the ‘lid’ is similar to that for a circular plate clamped around the edges (not, as previously assumed, a beam).

Then, the tensile stress developed in the plate is

$$\sigma_t = \frac{3pD_k t_w^2}{16l_v^2}$$

where $p$ = unit distributed load on clamped circular plate
$\rho_{\text{solid}}$ = density of solid rock
$\rho_{\text{soil}}$ = density of overlying soil
$\sigma_t$ = tensile stress generated in lid (clamped circular plate)
$\sigma_{tm}$ = tensile strength of lid
$t_w$ = thickness of weathered material on top
$t_l$ = thickness of unweathered rock above top of collapse cone.

**Bulking factor**

All the equations used for estimating the height of collapse are sensitive to the bulking factor. A commonly used guide is the one from Canbulat et al., (2002), contained in Table III, which originated from two older publications, Steijn (1983) and Wagner and Steijn (1979), reported in Sweby (1997). The data originated from measurements in longwall goafs, which can be expected to be different from collapses in a more confined area, such as a roadway intersection collapse. Less rotation of the broken rock and thus lower bulking factors can be expected in the latter case – this is borne out by observation of rubble at roof collapse sites (see Figure 7). This is the only data gathered in South Africa to date, but it should be used with caution; using bulking factors greater than 1.25 are not advised and the conservative approach, in the absence of measured data on any specific mine, is to use a value of 1.1.

A comprehensive overview of bulking factors worldwide is given by Ofoegbu, Read, and Ferrante (2008). However,

| Table III |

| Bulking factors observed in longwall goafs (after Sweby, 1997) |
|-----------------|-----------------|-----------------|-----------------|-----------------|-----------------|-----------------|-----------------|
| Liqthology      | Coal            | Silt mudstone   | Carb. shale     | Shale           | Sandy shale     | Shaly sandst.   | Coarse sandstone | Fine sandstone  |
| Bulking factor  | 1.3             | 1.1             | 1.15            | 1.2             | 1.25            | 1.3             | 1.4             | 1.5             |

Figure 7—High-extraction goaf with rotation of debris and high bulking (left), contrasted with less rotation and less bulking of debris from a roof collapse (right)
almost all the data presented is relevant to longwall mining goafs or bulk mining methods. A very wide range of bulking factors for the same rock types is reported, which highlights the need for local observation. Esterhuizen and Karacan (2007) used analytical methods to predict bulking factors based on the ratio of block size to collapse height. They found that as the fall height decreases towards the top of the collapse zone, the bulking factor decreases. This finding supports the notion that lower bulking factors may be expected at lower mining heights for the same rock types.

The above two findings lead to the conclusion that it may be misleading to use generic bulking factors and that local measurement at roof fall sites should be the preferred method to determine the input parameters.

The caving angle, $\phi$

The conical collapse model is sensitive to the caving angle. If possible, this should be determined by observation at roof collapse sites or existing sinkholes. The only existing data in the public domain comes from longwall goaf observations, Sweby (1997) (see Table IV). Sweby (1997) remarked that the caving angle tends to be steeper for weaker rock masses, but did not quantify this statement – most probably due to lack of sufficient information.

Note that in all cases, the caving angle is shallower where the dolerite sill has not failed, presumably due to the restraining effect of the intact sill at the top of the cavity. Observations at roof collapse sites over several years tend to support the caving angles given in Table IV, but this type of observation can be misleading as such observations are usually made very soon after the collapse occurred and the possibility that the angles may become steeper over time cannot be ignored.

Figure 8 illustrates the effects of bulking factor and caving angle on the maximum height of the cavity based on the conical model. It was constructed for a case where the bord width was 6 m and the mining height 3 m. It is seen that for a vertical caving angle (caving angle 0°), the height of the cavity decreases with increasing bulking factor. For small caving angles, the height of the cavity increases with increasing caving angle, reaching a maximum at the point where the termination mechanism changes from bulking to wedging. From that point onwards, the caving height decreases with increasing caving angle and the bulking factor is no longer relevant.

This may explain why sinkholes are rarely experienced at mining depths of more than 20 m. It would appear that the caving height of roof collapse is controlled more by the caving angle than by the bulking factor. This also emphasises the need for site-specific observation and measurement rather than relying on estimated or generic values.

Subsurface erosion (SSE)

Subsurface erosion (SSE) is a time-dependent phenomenon that occurs around the perimeter of trough subsidence after a period of a year or more, sometimes up to 100 years after mining. It appears at the positions where tensile cracks formed as a result of the tensile strains caused by trough subsidence.

SSE develops by a process whereby soil is washed into cracks in the upper rock layers, resulting in a subterranean cavity that eventually collapses, forming a small sinkhole. This type of sinkhole is distinct from classical sinkholes caused by underground roof collapse. SSE sinkholes tend to be smaller, usually less than a metre in diameter – although in exceptional cases holes more than 5 m diameter have also been found. A superficial description of the phenomenon is contained in van der Merwe and Madden (2010). However, that description does not include sufficient detail of the process to enable one to isolate the critical factors. More detail is given in the following paragraphs.

For SSE to occur, the following are required:

- Erodible soil
- Crack in rock (reservoir for eroded soil)
- Layered soil (e.g. clay and sand in layers)
- Transport medium (water).

Based on observation, the end result is a cavity with cross-section as shown in Figure 9.

Maximum width of cavity based on volume considerations

Based on volume comparison, the volume of eroded soil in the crack must equal the volume of the cavity in the soil. Then the maximum width of the sinkhole, provided the lid is strong enough to span across the cavity, is given by:

$$w_h = \frac{1}{2} \tan (\phi) \left( t_1 - t_2 - \left( \frac{1}{2} \tan (\phi) t_1 + t_2 \right)^2 - \left( \frac{1}{2} \tan (\phi) t_1 + t_2 \right)^2 \right)$$

where $w_h$ = cavity width

![Figure 8—Cavity height as a function of the bulking factor and caving angle](image)

Table IV

Caving angles measured at different sites (based on Sweby, 1997)

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<td>13–18° (average 16°)</td>
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<td>Coalbrook</td>
<td>18–22° (average 16°)</td>
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<td>Bojespruit</td>
<td>38–51° (average 43°)</td>
<td>24–43° (average 29°)</td>
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<tr>
<td>DNC</td>
<td>17–21° (average 20°)</td>
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<tr>
<td>Brandspruit</td>
<td>18–28° (average 25°)</td>
<td></td>
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</tbody>
</table>
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\[ \tau = \frac{2 \pi t C + \sqrt{(2 \pi t C)^2 - 4Ft \rho g \tau}}{2 \pi t \rho g} \]  \hspace{1cm} \text{(m)} \hspace{2cm} \text{[30]}

where \( C \) = cohesion of the lid material (kPa) \( \rho \) = density of the material (kg/m\(^3\)) \( F \) = point load (kN).

Simple beam failure
The maximum width of the cavity based on a combined loading system of a uniformly distributed load and a point load is given by

\[ w_h = \frac{-1.5F + \left( \frac{1.5F}{t_s^2} \right)^2 - \frac{0.9 \rho g \tau^2}{t_s^2}}{1.5 \rho g t_s^2} \]  \hspace{1cm} \text{(m)} \hspace{2cm} \text{[31]}

Circular plate failure
For this failure mode, based on the fundamental consideration of tensile stress developed around the edge of a clamped circular plate with a combination of a uniformly distributed load and a point load, the radius of the plate resulting in failure is easiest found by iteration of the equation

\[ \sigma_t = \frac{F}{t_s} \left( 1 + v \right) \left( 0.485 \frac{\pi r^2}{t_s} + 0.52 \right) + 0.48 \]  \hspace{1cm} \text{(m)} \hspace{2cm} \text{[32]}

where \( \sigma_t \) = tensile strength of the upper layer making up the lid of the cavity \( v \) = unit distributed weight (\( \rho g t \)).
The procedure for using the equations in this section is to firstly calculate the maximum width of the cavity using Equation [29] and then check the effect of the strength of the lid with Equation [32]. The reason for using Equation [32] for the lid evaluation is that the first failure is invariably circular (see Figure 12), with the beam-type failure following, linking up the circular failures.

**Typical input values**

Equations [29] and [32] require a number of input values that are not always easy to determine. The following guidelines may be used.

- Crack width, $w_c$. The beacon spacing used for the subsidence research described by van der Merwe (1991) was 5 m, and consequently the total elongation at the positions where the maximum tensile strain occurred was $5 \times$ maximum tensile strain (see Equation [8]) or $21S_{\text{Sm}} + 6.5$ (mm). It is reasonable to equate this to the crack width in the rock.

- Crack depth, $t_c$. It has been seen that the cracks in the rock are not necessarily continuous, but are often laterally displaced on bedding planes. This means that the cracks are unlikely to extend in a straight line all the way down to the goaf. Back-analysis of known SSE cavity widths tend to indicate that a maximum continuous crack depth of 25 m is reasonable to use.

- Soil depth, $t_s$. This is measurable, but it should be borne in mind that the depth to which the crack is likely to have reached is of more significance.

- Lid thickness, $t_l$. This is likewise measurable and would typically be the thickness of an upper clayey layer (if it exists) or the thickness of the upper soil layer which is compacted by animal or human movement and strengthened by grass roots. If in doubt, use a value of 0.1 m.

- Poisson’s ratio of soil, $v$. (Bowles, 1996):
  - Clay, saturated: 0.4 to 0.5
  - Clay, unsaturated: 0.1 to 0.3
  - Sandy clay: 0.2 to 0.3
  - Silt: 0.3 to 0.35
  - Sand, gravelly sand: 0.1 to 1.0 (not elastic, but 0.3 to 0.4 commonly used)

- Tensile strength of lid, $\alpha_t$. Where the lid consists of constructed material like a concrete slab or a road pavement, this parameter is measurable. If it is soil, it can be likened to a fibre-reinforced soil (effect of plant roots), in which case a tensile strength of 50 kPa to 80 kPa appears reasonable to use (Li et al., 2004).

Although a detailed study using the default input numbers shown has not been undertaken, the results indicate reasonable agreement with observations at subsidence sites in the Vaal Basin and Highveld coalfields. The default values with a soil thickness of 1 m and point load of 80 kg indicated a cavity width of approximately 1 m, comparable with the examples shown in Figure 12.

**Conclusions**

A number of updates of procedures commonly used to evaluate the stability of the surface overlying coal mine workings have been presented.

Instead of just a single profile of subsidence in the case of high-extraction mining, upper and lower limits based on re-analysis of existing data are given. This makes it possible to evaluate whether more subsidence can be expected in places where subsidence has already occurred, and also to predict the expected limits of subsidence in new mining areas. An improved method of predicting the expected amount of subsidence in the case of pillar extraction is also presented.

Predictions of the maximum amounts of subsidence and induced tilt in the case of pillar failure are given, based on analysis of an improved database that incorporates additional subsidence cases that occurred after the previous method was published.

A more realistic method of evaluating the possibility of sinkhole formation is presented. This is based on the truncated cone model as opposed to the previously used cylindrical model. With this model, two mechanisms of cavity arrest are evaluated, namely choking of the cavity by bulking of the collapsed material and wedging out of the cone before it reaches the surface.

A method to estimate the maximum width of subsurface erosion cavities is presented for the first time. This takes account of the maximum volume of the receptacle for the eroded soil as well as the strength of the overlying lid of the cavity.

These updates illustrate the need for periodic review of all empirically derived methods. As time goes by, databases continue to expand, resulting in progressively better methods of prediction. Improved methods of prediction also arise from better understanding of the processes involved.

The equations presented in the paper are more complex than those previously used, but no specialist software is required for the calculations. All can be performed easily with commonly available tools such as Microsoft Excel™.

Suggested default input values are presented but should only be used as a last resort. For instance, the caving angle and bulking factor are crucially important for the prediction of sinkhole formation. Very few, if any, mines would have this information available, but both these factors are relatively easy to obtain by observation at roof collapse sites underground.

To quote Steve Crouch, co-developer of the boundary element methods of analysis: ‘If you are going to guess the input, why not just guess the answer?’
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In 1938, Charles Warman came up with an idea that would totally revolutionise the mining industry. As soon as Warman’s pumps came on to the scene, they became the very best you could buy. If it wasn’t Warman, you were wasting your time.

Over the course of 80 years, Warman pumps spread across the globe driven by the ethos to always innovate. Soon, Warman became a global brand with a bigger influence than even the forward-thinking Charles Warman could ever have imagined. All over the world, Warman is known as the very best.

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4th YOUNG PROFESSIONALS CONFERENCE
CREATING A SUSTAINABLE AFRICAN MINERALS INDUSTRY THROUGH APPLIED INNOVATION
18–19 September 2018, Focus Rooms, The Core, Sunninghill, Sandton

BACKGROUND
The minerals industry is a backbone for most of the economies in the African continent, thus, it is vital that the exploitation of the mineral resources is conducted profitably. However, without any paradigm shift in the tactics we employ to mine these resources, the mineral wealth we possess will not be of any benefit to the current and future generations. There needs to be a shift in the way we exploit the resources in order to ensure longevity of current operations and enable mining of deep-level complex orebodies in a safe, healthy and profitable manner. This can be achieved through integration of 1st, 2nd, 3rd and 4th revolutions to create a sustainable minerals industry.

OBJECTIVE
➢ Provide proof of the ‘unlocked value’ offered by innovations through technology (tools), strategy and skills development.
➢ Offer deeper context specifically for the African minerals sector (risks & opportunities)
➢ Document impact of current/anticipated changes on future sustainability and competitiveness
➢ Offer clarity on expectations for papers/abstracts submission through announcements

WHO SHOULD ATTEND
This conference should be of value to all professionals across the entire minerals industry value chain, including:
➢ All metallurgical fields
➢ Exploration
➢ Geology
➢ Geotechnical engineering
➢ ICT experts
➢ Industrial relations and community involvement, mechanical, electrical/electronic engineers

➢ Leadership/management/government/community
➢ Mineralogy
➢ Mining
➢ Mixed reality visualisation tech
➢ SHE practitioners
➢ Software developers

HOW TO REGISTER
Complete the registration form and return to anna@saimm.co.za or camielah@saimm.co.za

EXHIBITION/SPONSORSHIP
Sponsorship opportunities are available. Companies wishing to sponsor or exhibit should contact the Conference Co-ordinator.

FOR FURTHER INFORMATION CONTACT:
Camielah Jardine, Head of Conferencing: E-mail: camielah@saimm.co.za
Tel: +27 11 834 1273/7 • www.saimm.co.za