Large-Scale In Situ Heater Tests for Hydrothermal Characterization at Yucca Mountain

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ABSTRACT

To safely and permanently store high-level nuclear waste, the potential Yucca Mountain repository site must mitigate the release and transport of radionuclides for tens of thousands of years. In the failure scenario of greatest concern, water would contact a waste package, accelerate its failure rate, and eventually transport radionuclides to the water table. Our analyses indicate that the ambient hydrological system will be dominated by repository-heat-driven hydrothermal flow for tens of thousands of years. In situ heater tests are required to provide an understanding of coupled geomechanical-hydrothermal-geochemical behavior in the engineered and natural barriers under repository thermal loading conditions. In situ heater tests have been included in the Site Characterization Plan in response to regulatory requirements for site characterization and to support the validation of process models required to assess the total systems performance at the site. The success of the License Application (LA) hinges largely on how effectively we validate the process models that provide the basis for performance assessment. Because of limited time, some of the in situ tests will have to be accelerated relative to actual thermal loading conditions. We examine the trade-offs between the limited test duration and generating hydrothermal conditions applicable to repository performance during the entire thermal loading cycle, including heating (boiling and dry-out) and cooldown (re-wetting). For in situ heater tests to be applicable to actual repository conditions, a minimum heater test duration of 6-7 yr (including 4 yr of full-power heating) is required. The parallel use of highly accelerated, shorter-duration tests may provide timely information for the LA, provided that the applicability of the test results can be validated against ongoing nominal-rate heater tests.

INTRODUCTION

The Yucca Mountain Site Characterization Project (YMP) of the U.S. Department of Energy (DOE) is investigating the suitability of the fractured, tuffaceous rocks occurring in the unsaturated zone (UZ) at Yucca Mountain, Nevada, for high-level nuclear waste storage. In the failure scenario of greatest concern, water would contact the waste package (WP), accelerate its failure rate, and eventually transport radionuclides to the water table. Prior to 1991, efforts in hydrological performance assessment modeling largely focused on either far-field infiltration under ambient conditions or hydrothermal flow in the near-field WP environment. Recent advances in the modeling and theoretical understanding of nonisothermal, multi-phase fracture-matrix flow have demonstrated two things: (1) the critical importance of capillary pressure disequilibrium between fracture and matrix flow, and (2) the dominant role played by radioactive decay heat in the ability of the engineered and natural barriers to contain and isolate radionuclides. Recent studies have shown that repository-heat-driven hydrothermal flow will dominate the ambient hydrological system in the UZ and saturated zone (SZ) for tens of thousands of years.

With respect to thermo-hydrological performance, an important macroscopic thermal loading parameter that quantifies the integral amount of thermal energy per unit area of repository is the Areal Mass Loading [(AML) expressed in metric tons of uranium per acre, MTU/acre]. For high AMLs (resulting in long-term boiling and sub-ambient saturation conditions), we find that liquid-phase flux associated with vapor flow and condensate drainage during the boiling period, as well as re-wetting of the dry-out zone during the post-boiling-period, is much greater than the net recharge flux associated with pluvial climatic conditions. Even for low AMLs (which have insignificant dry-out due to boiling), repository-heat-driven hydrothermal flow will dominate SZ flow and also local flow conditions throughout much of the UZ. This is not to say that episodic nonequilibrium fracture flow (from meteoric sources) is not important. For sub-boiling conditions, when episodic events occur, fracture
flow is likely to be the dominant source of liquid flux along preferential pathways (connected to meteoric sources). Overall, however, repository-heat-driven hydrothermal flow is likely to be the major contributor of gas- and liquid-phase flow in the UZ. Therefore, the most important consideration in determining whether the Yucca Mountain site is suitable for the emplacement of heat-producing, high-level nuclear waste is how heat moves fluid already present at Yucca Mountain; the impact of water that has yet to infiltrate at Yucca Mountain is of secondary importance for high AMLs, and is, at best, of equal importance for low AMLs.

This report describes the modeling and analysis of the hydrothermal performance of in situ heater tests. The primary purpose of this analysis was to support the Heater Test Duration Task Force (HTDTF), which was made up of representatives from each of the major YMP participants. The goal of the HTDTF was to address the issue of whether the current schedule for the Exploratory Studies Facility (ESF) at Yucca Mountain allows for adequate heater test duration prior to the 2001 License Application Deadline (LAD), or whether an earlier start date is required to meet the LAD. Early heater testing could be conducted in the proposed Yucca Mountain Test Facility (YMTF) at Busted Butte. The other major objective of this study was to evaluate the ability of the in situ heater tests to answer the key hypothesis tests required in validating the process models that provide the basis for performance assessment. We will discuss how, during the course of highly accelerated and nominal-rate in situ heater tests, the information obtained will progressively resolve critical questions concerning (1) the dominant mode of heat flow, (2) boiling, dry-out, and condensate drainage, and (3) re-wetting performance. The tests will also help us determine how these issues impact WP integrity and radionuclide transport.

We apply the hydrologic and thermal properties and hydrostratigraphy used in previous modeling studies of repository-heat-driven hydrothermal flow. Because of the similarity in hydrostratigraphy between Busted Butte and Yucca Mountain, our calculations are applicable to heater tests at Busted Butte as well as at the ESF in Yucca Mountain. Because of limited time prior to the LAD, these tests will have to be accelerated. In this study, we examine the trade-offs between (1) meeting the LAD, (2) hydrothermally perturbing a sufficiently large volume of rock relative to the spatial variability of the fracture and matrix properties, and (3) generating hydrothermal, geochemical, and geomechanical conditions that are applicable to repository performance during the entire thermal loading cycle, including heating (boiling, dry-out, and condensate drainage) and cooldown (re-wetting).

Purpose of Heater Testing

In situ heater tests conducted under thermal loading conditions that are reasonably representative of repository conditions are required to provide an understanding of coupled processes, including:

1. Thermo-hydrological behavior, with emphasis on identifying the dominant mode of heat flow; boiling, dry-out, and re-wetting performance; buoyancy-driven gas- and liquid-phase flow; and heat-driven alteration of intrinsic hydrologic properties.
2. Geochemical behavior, with emphasis on its impact on altering intrinsic hydrologic properties and the resulting chemistry of water that returns to WP locations.
3. Geomechanical behavior, with emphasis on heat-driven opening and closing of fractures and the initiation of new fractures.

To achieve these objectives, several tests have been described in the Site Characterization Plan (SCP). In order to understand the many coupled processes that are likely to occur in the host rock, and to adequately sample the variety of rock types required in assessing the performance of the repository system, more than one type and location of test is likely to be required. The sufficiency of any test (with respect to scope, size, and duration) for making findings on the compliance of the site with the performance objectives will depend on adequate representation of the inhomogeneities and discontinuities of the rock being tested. At present, the performance allocations focus on ambient flow in the natural barriers in the UZ (with an emphasis on the interval between the repository horizon and the water table). However, because heat-driven flow of in situ fluids already present at Yucca Mountain constitutes the most significant source of liquid flux at the repository horizon, heat-driven gas- and liquid-phase flow in the UZ must also be understood.
Factors Affecting Heater Test Size and Duration

The required size and duration of in situ heater tests are influenced by regulatory requirements, model validation needs, and the need for scientific credibility. The Nuclear Regulatory Commission (NRC) requirements relevant to in situ testing are found in 10 CFR 60.10 Site characterization must include in situ exploration and testing at the depths that WPs would be emplaced unless the Commission determines this is not necessary for a specific site (10 CFR 60.15). The results of in situ testing will be used to develop the information contained in the Safety Analysis Report (SAR) that will be part of the License Application (LA). The SAR must include a description of the geomechanical, hydrological, and geochemical properties and conditions of the site, and the anticipated response of these systems to the maximum design thermal load of the repository (10 CFR 60.21(c)(1)(ii)(C and F)).

The assessment of the site presented in the SAR must include an evaluation of repository performance and a description of the measures used to support the models that provide the basis for performance assessment. The analyses must be supported by an appropriate combination of in situ tests, laboratory tests representative of field conditions, monitoring data, and natural analog studies (10 CFR 60.21(c)(1)(ii)(C and F)). The NRC issued their "Generic Technical Position on In Situ Testing During Site Characterization for High-Level Nuclear Waste Repositories" (GTP) to provide guidance to the DOE on in situ testing. The introductory sections of the GTP state that in situ tests should represent a realistic repository environment as closely as possible. However, it was recognized that the time frame available for these tests is very limited in comparison with post-closure performance. The NRC staff specifically noted that there is a "deficient understanding of the effects of heat on rock and mineral behavior as well as the induced hydrological and geochemical changes." The results of in situ tests can be used to validate the models and reduce the uncertainties in the prediction process.

Overview of Unsaturated Zone Hydrology at Yucca Mountain

Yucca Mountain consists of a series of variably fractured, nonwelded to densely welded tuff units with an eastward tilt of 5 to 30 deg. The UZ thickness varies from 500 to 750 m. The potential repository location is in Topopah Spring (TSw2) moderately to densely welded tuff, which is about 350 m below the ground surface and 225 m above the water table. Montazer and Wilson report the absence of perennial streams at Yucca Mountain. Therefore, recharge due to rainfall or snowmelt occurs episodically.

The matrix properties of the hydrostratigraphic units at Yucca Mountain are summarized by Klavetter and Peters. The units generally fall into two categories: (1) the welded tuffs (TCw, TSw1, TSw2, and TSw3) and the nonwelded zeolitized unit (CHnz), all of which have low matrix permeability, \( k_m \), and low-to-medium matrix porosity, \( \phi_m \), and (2) nonwelded vitric tuffs of high \( k_m \) and \( \phi_m \) (PTn and CHnv). The welded PPw has medium \( k_m \) and \( \phi_m \). The \( k_m \)'s of the nonwelded vitric tuffs are 4 to 5 orders of magnitude greater than those of the welded tuffs and the CH nz.

Infiltration at Yucca Mountain has been modeled with a one-dimensional steady-state equivalent continuum model (ECM) for recharge fluxes of 0, 0.045, and 0.132 mm/yr, resulting in repository horizon saturations of 68, 85, and 95%, respectively (Fig. 1). On the basis of the assumption of instantaneous capillary pressure equilibrium between fracture and matrix, the ECM volume-averages the fracture and matrix into an equivalent (or effective) continuum. Saturation values obtained from the Reference Information Base (RIB) are also included in Fig. 1. Notice the good agreement between the saturation profile calculated for zero recharge flux and the RIB values in the low-\( k_m \) units (TCw, TSw1, TSw2, TSw3, CHnz, and PPw). The RIB values are significantly higher than the calculated profile for the high-\( k_m \) units (PTn and CHnv). The saturation profile in the small-\( k_m \) units is very sensitive to variations in recharge flux. Because of their large \( k_m \), the PTn and CHnv can sustain a relatively large steady-state recharge flux at small saturations. Nonequilibrium fracture flow through the TCw, TSw1, TSw2, and TSw3 is a likely cause for the apparent inconsistency between the RIB data in the PTn and CHnv and the saturation profile predicted by the one-dimensional, steady-state ECM.

Numerous performance assessment calculations have been carried out for the potential Yucca Mountain repository site. Most of these have utilized a steady-state ECM that, for the assumed recharge fluxes, effectively constrains flow to be matrix-dominated. Because of the very low \( k_m \) prevalent through most of the UZ, the steady-state ECM calculations have repeatedly shown that matrix-dominated flow will not result in significant vertical transport of radionuclides for thousands of years.
Moreover, field evidence indicates that fracture-dominated flow can occur to considerable depth. Therefore, fracture-dominated flow is the only credible mechanism capable of bringing water to the WPs and transporting radionuclides to the water table. The impact of repository-heat-driven hydrothermal flow on fracture-dominated flow is critical to repository performance.

Given that liquid flow along preferential fracture flow paths provides the most likely means of generating adverse WP conditions and transporting radionuclides to the water table, three general features, or mechanisms, tend to mitigate the potential release and transport of radionuclides. The first is discontinuity in preferential fracture flow paths. The second is liquid-phase dispersion in fracture networks, which results as flow branches from a preferential fracture pathway into lateral fractures. The third mechanism is fracture-matrix interaction. For thermal loading conditions that result in sub-boiling conditions, the predominant mode of fracture-matrix interaction is capillary-driven fracture-to-matrix flow, called matrix imbibition. For AMLs resulting in marginal boiling conditions, the variability in the heat generation rate among the WPs may result in condensate flow from hotter to cooler WPs, increasing the adverse effects of fracture flow. For higher AMLs, boiling effects and enhanced imbibition resulting from rock dry-out will significantly add to the capacity of the UZ to retard fracture-dominated flow.

Hydrothermal Flow at the Repository Horizon

Much of our current understanding of repository-heat-driven hydrothermal flow in unsaturated fractured tuff is based on observations made during the heater tests in G-Tunnel and associated modeling studies. For drift emplacement without backfill, the primary mode of heat transfer from the WP to the wall of the emplacement drift is thermal radiation (Fig. 2). If backfill is present, heat flow through the backfill will be dominated by heat conduction.

Both the G-Tunnel heater test modeling analysis and the performance analysis of repository-heat-driven hydrothermal flow have indicated that heat flow in the UZ will be dominated by heat conduction. Because of the large bulk permeability of fracture networks, gas-phase pressures, \( p_g \), in the fractures remain very close to atmospheric, even during boiling. Consequently, as temperatures reach the nominal boiling point \( T_b \), boiling first occurs along the fracture faces, i.e., the perimeter of the matrix blocks, and proceeds in toward the center of the matrix blocks. Because of the small \( k_m \) at the repository horizon, boiling results in large \( p_g \) gradients (with \( p_g \) increasing from the perimeter to the center of the matrix blocks), causing a rise in the boiling temperature, \( \Delta T_b \), above the nominal boiling point, \( T_b \). Because \( p_g \) gradients increase with matrix block size, \( \Delta T_b \) increases with matrix block size. Accordingly, boiling is more suppressed in sparsely fractured regions and less suppressed in intensely fractured regions. For regularly spaced fractures, the volume of the dry-out zone was found to be proportional to \( k_m^B \), where \( B \) is the effective fracture spacing. For the G-Tunnel test, \( B \) was apparently small enough (relative to \( k_m \)) that the effect of \( \Delta T_b \) on rock dry-out was negligible. The sensitivity of the dry-out volume to fracture aperture, \( b \), was also examined. For \( b > 20 \mu m \), the volume of the dry-out zone was found to be insensitive to \( b \). For the fractures observed in G-Tunnel, flow resistance in the fractures does not appear to throttle the rate of dry-out.

At early time, a small percentage of the water vapor that reaches the fracture network in the immediate vicinity of the WP flows back toward the emplacement drift (Fig. 2). Otherwise, most of the water vapor reaching the fracture network is driven (by higher \( p_g \) in the boiling zone) away from the emplacement drift to where cooler temperatures cause it to condense along fracture walls. Because the small \( k_m \) limits the rate of matrix imbibition, condensate drainage down fractures persists for considerable distances before being imbibed by the matrix. In the region below the boiling zone, condensate drainage is away from the boiling front (Fig. 2), enhancing the rate of rock dry-out. Above the boiling zone, condensate tends to drain back toward the boiling zone, thereby retarding the net rate of dry-out (Fig. 2).

During the G-Tunnel test, regions above the heater were observed to dry out more slowly than regions below the heater. Because vapor flow in fractures tends to be radially away from the heat source and condensate drainage is vertically downward, successive refluxing cycles in fractures eventually result in condensate being shed off the top and down the sides of the boiling zone (Fig. 3). This "hydrothermal umbrella" effect was manifested during the G-Tunnel test in the temperature history of several thermocouples along the flank of the boiling zone. This concept was first mentioned by...
Roseboom in the context of emplacing high-level waste in the UZ. The "umbrella" mechanism may significantly enhance near-field dry-out, at least until the dry-out zones of neighboring emplacement drifts (or panels) have coalesced.

The Use of Hypothesis Testing in Model Validation

Since the beginning of geologic repository studies, it has been recognized that the primary pathway for potential release of radionuclides is groundwater flow. For a repository located in the UZ, the primary concern is liquid-phase flow because it can impact both the integrity of the WPs as well as mobilization and transport of radionuclides to the water table. However, gas-phase transport of radionuclides (e.g., $^{14}$C) is also being considered. Results from \textit{in situ} tests are required to validate models that will be used to predict flow through the UZ at Yucca Mountain under both pre- and post-waste emplacement conditions. Because of the dependence on models, the success of the LA hinges largely on how effectively we can validate the detailed flow models that provide the basis for performance assessment.

The licensing of a low- to medium-AML repository, which generates sub- or marginal-boiling conditions with insignificant dry-out, will be critically dependent on characterizing the highly heterogeneous distribution of fracture and matrix properties as well as the highly spatially and temporally variable distribution of net recharge flux. Licensing will also depend on the validity of hydrological models that account for (1) these very complex, variable distributions, (2) the strongly nonlinear dependence of fracture flow on these heterogeneous distributions, (3) how the spatial variability in the heat generation rate among the WPs will drive condensate flow from hotter to cooler WPs (and possibly to the water table), and (4) the impact of hydrothermal-geochemical coupling on the flow and transport properties as well as on the chemistry of water contacting WPs. Because sub-boiling or marginal-boiling conditions will not mitigate the occurrence of deep nonequilibrium fracture flow (from meteoric sources), hydrologic assessments of low thermal loading conditions must account for the superposition of naturally occurring episodic fracture flow and repository-heat-driven refluxing for essentially the entire time of regulatory concern.

For high AMLs, it has been shown that it may be possible to maintain boiling conditions at the repository horizon for 10,000 yr, with re-wetting of the repository back to ambient saturation conditions taking in excess of 100,000 yr. The licensing of a high-AML repository, which generates long-term boiling conditions with significant dry-out, can be based on three fundamental considerations: (1) the spatial and temporal extent of above-boiling conditions, (2) how closely the dry-out zone corresponds to the zone of above-boiling conditions, and (3) how long it takes the dry-out zone to re-wet back to ambient saturation. The validation of the performance of the extended-dry concept is greatly facilitated by addressing several fundamental hypothesis tests:

(1) whether heat conduction dominates heat flow,
(2) whether fracture density and connectivity are sufficient to promote rock dry-out due to boiling and condensate shedding, and
(3) whether re-wetting of the dry-out zone back to ambient saturation significantly lags behind the end of the boiling period.

In addressing these hypotheses, we also ask whether hydrothermal-geochemical-geomechanical coupling must be dynamically accounted for in performance models or whether it can be conservatively accounted for by bounding analyses. The use of these hypothesis tests can greatly focus the critical characterization, modeling, laboratory, and \textit{in situ} testing activities required in building robust site suitability and licensing arguments. The validation of these hypotheses will profoundly reduce the impact of hydrogeological uncertainty and variability on the predictability of total system performance.

The most conclusive means of testing these hypotheses involve large-scale \textit{in situ} heater tests at various hydrostratigraphic intervals of the UZ. \textit{In situ} heater tests will also be extremely useful in determining whether temperature rise, condensate flow, and buoyancy-driven SZ flow can drive geochemical changes that significantly alter properties within the Engineered Barrier System (EBS) and Natural Barrier System (NBS). Critical performance issues involving hydrothermal-geochemical-geomechanical coupling cannot be entirely resolved either in the laboratory or through modeling. Moreover, critical hydrological performance issues cannot be entirely resolved by ambient property
measurements conducted during site characterization. Therefore, in situ heater tests at various hydrostratigraphic intervals (above as well as below the repository horizon) will be critical to addressing such issues.

**Physical Criteria Affecting Heater Test Size and Duration**

Because of limited time prior to the LAD, the in situ heater tests will have to be accelerated relative to actual thermal loading conditions. A major objective of this study is to examine the trade-offs between test duration (and heating rate) and generating hydrothermal conditions applicable to repository performance during the entire thermal loading cycle, including heating (boiling and dry-out) and cooldown (re-wetting). For both the heating phase and cooldown phase, the heater test will accelerate changes in time and space, including

1. the transition from sub-boiling to boiling conditions,
2. the transition from boiling to above-boiling conditions, and
3. the transition from above-boiling to sub-boiling conditions.

It is important that the accelerated thermal cycle during the in situ heater test does not preclude the occurrence of coupled hydrothermal-geochemical-geomechanical phenomena that may be of critical importance to boiling, dry-out, and re-wetting performance under actual repository conditions. Because of the range of testing options (e.g., the ESF-only option vs the YMTF-ESF option), our calculations address heater tests that range from a 1.5-yr duration (with 1 yr of full-power heating) to a 6-7 yr duration (with 4 yr of full-power heating). During the latter stage of the tests, the heating rate is ramped down to zero in order to observe cool-down and re-wetting behavior. Because of the importance of performance confirmation testing, we also look at the additional information that can be obtained on a 20+ yr timeframe, including data provided by tests with longer-duration, full-power heating stages, as well as data obtained during the re-wetting phase of shorter-duration tests.

For determining the size and duration of the heater tests, we considered the following criteria:

1. the velocity of the dry-out front,
2. the size and duration of condensate perching,
3. the peak rock temperatures,
4. the time rate of change of temperature, and
5. the volume of the dry-out zone.

The first and second criteria primarily relate to hydrothermal-geochemical coupling at the refluxing front, which may result in geochemical alteration of fracture and matrix properties. If the dry-out front is driven too quickly, there will be inadequate time for geochemical effects to occur. Note that an increase in boiling (and dry-out rate) will also be accompanied by an increase in the return flux of condensate. Therefore, one might expect that the increased rate in condensate flow would compensate for the reduced overall time that a given region of fractured rock is exposed to refluxing conditions. If condensate shedding off the sides of the boiling zone occurs, then there will be a net loss of fluid from the refluxing system, enhancing the overall rate of dry-out. If condensate shedding is particularly effective, substantial overdriving of the dry-out rate (relative to applicable repository conditions) will have the effect of reducing the potential for reflux-driven redistribution of minerals in fractures, particularly silica. The third and fourth criteria relate to the potential of geomechanical and geochemical effects significantly altering the thermo-hydrological properties in a way that is not representative of repository thermal loading conditions.

The fifth criterion relates to the scale of the dry-out zone relative to the scale of the heterogeneity of the fracture properties (particularly the fracture spacing and connectivity). Because buoyancy-driven gas-phase convection has been demonstrated to be very dependent on the fracture system, it is necessary to dry-out a sufficient volume of rock to include an interconnected fracture system. Past estimates of this required volume were based on estimates of at least one fracture per foot horizontally and much less vertically. We estimated that a rock mass of about 10 m radius would be sufficiently large to have interconnected fracture clusters. If the dry-out zone is small relative to the scale of heterogeneity or connectivity, dry-out, buoyancy-driven gas-phase flow, and condensate drainage may be completely dominated by the local heterogeneity. Moreover, the validation of models that incorporate bulk
averages of matrix and fracture properties requires tests in which bulk averages are statistically meaningful. In order to hydrothermally perch condensate, it is necessary to coalesce the boiling zones, thereby precluding condensate from being easily shed off the sides of the individual boiling zones surrounding each heater.

**DISCUSSION OF NUMERICAL MODELS, PHYSICAL DATA, AND ASSUMPTIONS**

**V-TOUGH Hydrothermal Flow Code**

All calculations were carried out using the V-TOUGH ("vectorized transport of unsaturated groundwater and heat") code. V-TOUGH is Lawrence Livermore National Laboratory's enhanced version of the TOUGH code, which is a member of the Mulkom family of multiphase, multicomponent codes developed at Lawrence Berkeley Laboratory by Pruess. V-TOUGH is a multidimensional numerical simulator capable of modeling the coupled transport of water, vapor, air, and heat in fractured porous media.

**Equivalent Continuum Model**

Because of the impracticality of discretely accounting for all of the fractures within the repository block, it was necessary to account for fractures using the ECM. The assumption of capillary pressure and thermal equilibrium between fractures and matrix allows the fracture and matrix properties to be volume-averaged into an equivalent medium. The bulk porosity, $\phi_b$, bulk saturation, $S_b$, and bulk hydraulic conductivity, $K_b$, of the equivalent medium are given by:

$$\phi_b = \phi_f + (1 - \phi_f)\phi_m$$  \hspace{1cm} (1)

$$S_b = \frac{S_f \phi_f + S_m (1 - \phi_f) \phi_m}{\phi_f + (1 - \phi_f)\phi_m}$$  \hspace{1cm} (2)

$$K_b = K_m (1 - \phi_f) + K_f \phi_f$$  \hspace{1cm} (3)

where $\phi_m$, $S_m$, $\phi_f$, and $S_f$ are the porosity and saturation of the matrix and fractures, respectively. Because of the small $K_m$ in the UZ, $K_b$ is almost completely dominated by $K_f$ and $\phi_f$ for most fracture spacings and permeabilities.

In general, the ECM is appropriate for situations in which the fracture spacing is small enough to result in a negligible lag in the wetting or drying response in the matrix relative to that of the fractures. The use of the ECM implicitly assumes that flow resistance to liquid and vapor flow between the fractures and matrix is negligible. In the modeling analysis of the G-Tunnel heater test, the ECM-calculated saturation profile compared very well with the measured saturation in the dry-out zone. Apparently, the fracture spacing was sufficiently small so that, during boiling, the resistance to vapor flow from matrix to fracture was negligible. Although the ECM predicted a pronounced liquid saturation rise in the condensate zone, the G-Tunnel data showed no significant rise in liquid saturation outside of the boiling zone. With respect to matrix imbibition of condensate, the fracture spacing was not sufficiently small to validate the assumption of capillary pressure equilibrium in the condensate zone. Moreover, the lack of a rise in liquid saturation outside of the boiling zone, as well as some of the temperature data along the side of the boiling zone, indicated the "hydrothermal umbrella" effect of condensate shedding off the top and down the sides of the boiling zone.

The ECM-calculated temperature profile agreed very well with the measured G-Tunnel data. This close agreement was also obtained for a model that only accounted for heat conduction, indicating that heat flow around the G-Tunnel heater was dominated by heat conduction. On the basis of the G-Tunnel model validation effort, and given the similarity between the fracture and matrix properties of Grouse Canyon welded tuff in G-Tunnel and the Topopah Spring welded tuff at the repository horizon, the ECM should yield accurate predictions of temperature performance and conservatively low predictions of the spatial extent and duration of rock dry-out. In light of the capillary hysteresis effect observed for Grouse Canyon welded tuff, the ECM-calculated time required to re-wet the dry-out zone may be considerably less than actual.
Thermo-Hydrological Properties

All major hydrostratigraphic units in the UZ at Yucca Mountain are included in the models. This hydrostratigraphic profile has been used in previous modeling studies. We applied the RIB Version 3 K values, as was done in previous hydrothermal calculations. As in previous modeling studies, we assumed initial saturation conditions corresponding to the steady-state saturation profile obtained by Buscheck, Nitao, and Chesnut for a net recharge flux of 0 mm/yr, yielding a saturation of 68% at the heater test horizon (Fig. 1).

The reference- case assumed a bulk permeability, of 2.8 x 10^-19 m^2 (three 100-µm fractures per meter). The sensitivity of boiling and dry-out performance to was examined by considering a low- case, corresponding to no fractures ( ) and a high- case, corresponding to one 1000-µm fracture per meter ( ).

Initial and Boundary Conditions

The vertical distribution of temperature, T, of the models is initialized to correspond to the nominal geothermal gradient in the region. The atmosphere at the ground surface is represented by a constant-property boundary, with T and fixed at 13°C and 0.86 atm, respectively. The relative humidity at the ground surface is also fixed so that it is in vapor pressure equilibrium with the initial saturation conditions at the top of the TCw unit. Therefore, under initial (ambient) saturation and temperature conditions, there is no mass flux of water vapor between the atmosphere and upper TCw. As in previous work, we assumed that because of the large fracture permeability, buoyancy-driven convective mixing in the SZ results in the SZ acting as a heat sink. The large bulk permeability and storativity of the SZ were also assumed to result in the water table being at a fixed depth. Therefore, we assumed that the water table has a fixed depth (z=568.1 m) and a constant temperature (31°C). The radial distance to the outer model boundary is 15 km. At this boundary, the initial pressure, temperature, and liquid saturation distribution is maintained for all time. Buscheck and Nitao found that for the time scale of the calculations of repository-heat-driven hydrothermal flow, it is necessary to explicitly represent hydrothermal flow in the SZ in order to accurately account for heat flow crossing the water table. Because of the relatively short duration of the in situ heater tests, there is insufficient time for the heaters to interfere with the model boundaries. Therefore, the results of this study are insensitive to whether we treat the water table as a fixed-depth, constant-temperature boundary, or explicitly represent hydrothermal flow in the SZ.

The in situ heater tests are represented with two kinds of models. The drift-scale model is a two-dimensional cross-sectional model that explicitly represents the details of the heaters and heater drifts in the plane orthogonal to the drift axes. The "reference" test configuration is a 21-heater test that is comprised of three parallel 4.6-x-4.6-m heater drifts, with 12.8-m center-to-center drift spacing (Figs. 4 and 5). Each drift contains seven 1.5-x-1.5-x-4.6-m drift-emplaced heaters with 5.49-m center-to-center spacing between heaters. Note that the heaters are essentially emplaced end-to-end with a minimal gap in the axial direction. The heated length of each drift is 38.4 m. Because it is two-dimensional, the drift-scale model assumes that the heater drifts are infinitely long, effectively neglecting the heat loss that occurs due to heat flow parallel to the drift axes.

We also use an R-Z ("test-scale") model that represents the 21-heater test as a disk-shaped uniform heat source with a radius of 21.665 m (0.364-acre heated area) and a height of 1.5 m, yielding the same bulk-heated volume as the actual test configuration. The R-Z test-scale model smears temperature effects between the heater drifts at very early time, but it compares well with the cross-sectional drift-scale model after the dry-out zones have coalesced. The R-Z model has the advantage of accurately accounting for the overall heat flow. Comparisons between the cross-sectional drift-scale and R-Z test-scale models show outstanding agreement in rock temperature at the center of the heater array for the first 4 yr of full-power heating for 21 5.5-kW heaters (Fig. 6). The dry-out performance predicted by the two models also agrees reasonably well for t > 2 yr (Fig. 7). With the R-Z test-scale model, we also represented a 6-heater, 2-drift test as a disk-shaped heat source with a radius of 9.287 m (0.067-acre heated area), and a 65-heater, 5-drift test with a radius of 40.227 m (1.256-acre heated area).
DISCUSSION OF MODELING RESULTS

Overview of Modeling Study

In this section, we examine the sensitivity of thermo-hydrological performance of the heater tests to (1) test size (the number of heaters and heater drifts), (2) heating rate, and (3) $k_b$. In the first subsection, we apply the cross-sectional, drift-scale model (that explicitly accounts for the cross-sectional geometry of the heaters, the heater drifts, and the pillars separating the heater drifts) to the "reference" test configuration described above. Note that we defer discussing the analyses that led to the "reference" test size and duration until the last two sub-sections of this section. For the full-power heating period, we examine the detailed thermal, boiling, and dry-out performance predicted by the drift-scale model and compare it with the thermal and dry-out performance predicted by the $R-Z$ test-scale model, which uses a disk-shaped, uniform heat source to represent the heater configuration. After finding that the $R-Z$ test-scale model compares well with the cross-sectional drift-scale model, we continue the modeling study with the use of the $R-Z$ model.

The validation of thermo-hydrological models (particularly those for long-term boiling and dry-out performance) will be greatly facilitated by several hypothesis tests. In the second sub-section, we continue the investigation of boiling, dry-out, and condensate flow performance during the full-power heating period with the use of the $R-Z$ model. In both the first and second sub-sections, we address the ability of the in situ heater tests to resolve the second key hypothesis test, which concerns the question of whether fracture density and connectivity are sufficient to promote rock dry-out due to boiling and condensate shedding.

In the third sub-section, we consider a wide range of $k_b$ values in order to evaluate whether the temperature distribution developed during the course of the heater test will be a definitive indicator of the dominant mode of heat flow (ranging from heat-conduction-dominated to heat-convection-dominated). Therefore, we address the ability of the in situ heater tests to resolve the first and most critical hypothesis test, which concerns the question of whether heat conduction dominates heat flow.

In the fourth sub-section, we examine the thermal and re-wetting performance during the cool-down period that follows full-power heating. We address the ability of the in situ heater tests to resolve the third hypothesis test, which concerns the question of whether re-wetting of the dry-out zone back to ambient saturation lags significantly behind the end of the boiling period. In the last two sub-sections, we discuss the analyses of heater test size and duration.

Details of Boiling, Dry-Out, and Condensate Flow

The validation of thermo-hydrological models (particularly those for long-term boiling and dry-out performance) will be greatly facilitated by several hypothesis tests. We discuss the first and most critical hypothesis test (whether heat conduction dominates heat flow) in the sub-section on determining the dominant mode. Here, we address the ability of the in situ heater tests to resolve the second hypothesis test, concerning whether fracture density and connectivity are sufficient to promote rock dry-out due to boiling and condensate shedding. Our investigation begins with a detailed look at boiling, dry-out, and condensate flow in the vicinity of the heater drifts during the full-power heating period for the reference test configuration (Figs. 4, 5, and 8). The reference test includes three heater drifts with seven 5.5-kW heaters per drift. Heaters radiate heat to the floor, walls, and ceiling of the heater drifts. Heat is also conducted to the drift floor underlying the heaters. Notice that heat conduction accounts for much of the heat flow from the heaters, resulting in more rapid boiling and dry-out in the vicinity of the drift floor (Fig. 4a-b). After 1 yr of full-power heating, the dry-out zone (medium shaded area) is confined to the lower-half of the drift (Fig. 4a).

Notice the effect of thermal interference (with heating from the outer two drifts) enhancing the dry-out rate for the central heater drift (relative to the outer two drifts). Thermal interference from the outer (i.e., guard) heaters facilitates rapid dry-out in the center of the test without the need to substantially overdrive the temperatures around the central drift. Had we chosen to heat with just one heater drift, it would have required a much greater heater output (per heater) to achieve the same rate of dry-out, thereby resulting in a steeper temperature gradient in the dry-out zone. The use of multiple heater drifts allows a given rate of dry-out to be achieved at lower peak temperatures. Therefore, we find that it is critical for the heater test configuration to include at least three heater drifts.
The condensation zone (dark shaded area) surrounds the dry-out zone and completely envelops the upper one-third of the heat drifts. The condensation zones begin to coalesce in the pillars at \( t = 1 \text{ yr} \) (Fig. 4a). Notice that the condensation zones are associated with the flattening of the temperature profile (Fig. 8), with temperatures close to the nominal boiling point \( (T_b = 96^\circ C) \). The dry-out zone does not extend significantly beyond the drift ceiling for at least 3 yr (Figs. 4a–b and 5a). Therefore, the dripping of condensate from the drift ceiling (and onto the heaters) is likely for at least 3 yr. Notice that complete dry-out of the pillars does not occur for at least 3 yr. However, pillar temperatures (at the heater elevation) generally exceed 140°C at \( t = 3 \text{ yr} \); therefore, condensate drainage through the pillar will probably boil before reaching the lower condensation zone. Consequently, for \( t > 3 \text{ yr} \), condensate shedding will only occur around the perimeter of the 0.364-acre heated area.

The "hydrothermal" perching of condensate above the boiling zone is apparent in Figs. 4b and 5a–b. Because the ECM assumes capillary pressure equilibrium between the matrix and fractures, much of the condensate flow is constrained to occur in the matrix. Only the liquid saturation that is in excess of the critical saturation \( (S_t^c_{\text{en}} = 98.4\%) \) for fracture flow in the ECM is able to drain in the fractures. Consequently, the ECM substantially underpredicts condensate shedding that occurs as a result of capillary pressure disequilibrium between the matrix and fractures. Because the ECM limits condensate shedding, it also tends to underpredict the dry-out rate. Notice the concentration of condensate flow under the pillar at \( t = 2 \text{ yr} \) (Fig. 4b). These two regions of concentrated condensate flow eventually coalesce because of relative permeability effects that tend to bias the direction of condensate drainage (Fig. 5a–b). Because of nonequilibrium fracture-matrix flow, it is unlikely that condensate drainage will be focused into such a tight, well-defined area. In reality, condensate drainage will persist over a much wider region and to much greater depths below the heater test.

For the G-Tunnel Test, although the ECM could not accurately represent condensate flow, ECM-predicted dry-out behavior appeared to be quite accurate. Therefore, in this study, the ECM-predicted dry-out is probably also reasonably accurate. For the reference test configuration, coalescence of the boiling zones will effectively result in hydrothermally perching the condensate over a heated region of approximately 0.364 acres for at least the final year of the full-power heating period \( (3 < t < 4 \text{ yr}) \). As will be seen in the section on cooldown and re-wetting performance, boiling and condensate-flow effects will persist even after the heater power has been ramped to zero. Therefore, the hydrothermal perching of condensate will probably persist for some time into the cooldown period.

As previously mentioned, the thermo-hydrological performance predicted by the cross-sectional drift-scale model and the \( R-Z \) test-scale model agree well after a short period of time. At the center of the test configuration, we find outstanding agreement in the temperature history (Fig. 6). Because the drift-scale model neglects heat losses that are parallel to the heater axes, it eventually begins to over-predict temperatures for \( t > 5 \text{ yr} \) (Fig. 6). Neglecting the axial heat loss has a negligible effect on temperatures at the center of the test for at least 4 yr. Therefore, the drift-scale model is a reasonable representation of thermo-hydrological performance for a cross-section that is orthogonal to the heater axes and located at the mid-point of heating, i.e., the cross-section that cuts through the middle of the fourth heater (of the seven heaters in the heater drift).

For the prediction of dry-out performance, it takes approximately 2-3 yr before the two models agree reasonably well (Fig. 7). Because the heater drift is initially dry, the vertical dry-out zone thickness starts out equal to the height of the heater drift \( (h = 4.6 \text{ m}) \), causing the apparent discrepancy between the drift-scale and test-scale model at early time. Within 3 years, the predicted vertical thickness of the dry-out zone in the pillar overtakes the dry-out zone thickness predicted by the \( R-Z \) model (Fig. 7). At \( t = 4 \text{ yr} \), the geometry of dry-out and condensation predicted by the drift-scale model corresponds reasonably well to that which would be attributed to a disk-shaped, uniform heat source with the same overall heating rate. Therefore, for the remainder of this study we continue with the \( R-Z \) test-scale model, which represents the heater test configuration as a disk-shaped heat source with a uniform heating rate.

**Averaged Boiling, Dry-Out, and Condensate Flow**

In the following sub-sections, we apply the \( R-Z \) test-scale model, which represents the heater test configuration as a disk-shaped heat source with a uniform heating rate. Because this model averages the thermo-hydrological conditions between heater drifts, it can be considered to provide "averaged"
heater test performance. The validation of thermo-hydrological performance models (particularly those for long-term boiling and dry-out performance) will be greatly facilitated by several hypothesis tests. We deal with the first and most critical hypotheses test (whether heat conduction dominates heat flow) in the next sub-section. Here, we continue addressing the ability of the in situ heater tests to resolve the second hypothesis test, concerning whether fracture density and connectivity are sufficient to promote rock dry-out due to boiling and condensate shedding.

The effects of boiling, steam, and condensate flow are apparent in Figs. 9a–b and 10a–b. Notice that the temperature profile is flattened at the nominal boiling point \(T_b = 96^\circ C\). Steam is driven (by higher \(p_g\) in the boiling zone) away from the heater horizon to where cooler temperatures cause it to condense. Above the heater horizon, much of this condensate is driven back to the boiling zone, primarily by three processes: (1) capillary imbibition in the matrix, (2) capillary imbibition in small-aperture fractures, and (3) gravity drainage in fractures. Gravity drainage in the matrix is not significant in comparison with matrix imbibition, which is driven by very large capillary pressure gradients that arise from saturation gradients in the boiling and condensation zones. Below the heater horizon, only the first two mechanisms, capillary-driven flow in the matrix and fractures, contribute to condensate flow back toward the boiling zone, while the third mechanism, gravity drainage in fractures, tends to drain condensate away from the boiling zone.

The return flow of condensate back toward the boiling zone establishes a heat transfer mechanism (driven by the convection of latent heat) called the gravity-driven "heat pipe" effect. Given adequately high mass flux rates of the countercurrent flow of steam and condensate, heat pipes are capable of sustaining a given heat flux with a much shallower temperature gradient than is associated with heat conduction. Consequently, heat pipes are manifested by a very flat temperature profile, with temperatures close to \(T_b\) (Fig. 9b and c). Mass fluxes associated with gravity-driven fracture flow are much greater than those associated with capillary-driven flow in either the matrix or fractures. Therefore, above the heater horizon, gravity-driven condensate flow in fractures is the predominant source of liquid flow back to the boiling zone. While the flattening of the temperature profile above the heater horizon is primarily attributed to the gravity-driven refluxing or heat pipe effect, the flattening of the temperature profile below the heater horizon is attributed to condensate drainage convecting heat away from the boiling zone.

Classical heat pipes derive much of their thermal efficiency from their working fluid operating within a closed loop. As discussed earlier, the "hydrothermal umbrella" effect causes condensate to shed down the sides of the boiling zone.\(^{7,8,17}\) The loss of working fluid contributes to the migration of the boiling front away from the heat source because the return flow of condensate cannot keep pace with the rate of vaporization. Incidentally, regardless of whether working fluid is lost, an adequately high heat flux is capable of driving steam away from the heat source faster than the rate at which liquid water can return, thereby causing the dry-out front to advance. Condensate shedding results in a net loss of working fluid from the heat pipe, enhancing the rate at which the boiling front migrates away from the heat source. Because the ECM assumes capillary pressure equilibrium between fracture and matrix, condensate that would have drained freely under nonequilibrium fracture flow conditions is artificially held up in the matrix, where it is unable to drain freely away from the boiling zone. Consequently, the ECM tends to underpredict the dry-out rate.

**Determining the Dominant Mode of Heat Flow**

The validation of thermo-hydrological performance models will be greatly facilitated by several hypothesis tests, the most critical of which concerns the question of whether heat conduction is the dominant mode of heat flow. Heat convective processes are very dependent on the hydrological flow system, particularly gas-phase and liquid-phase flow in fractures. If heat convection were the dominant mode of repository-driven heat flow at Yucca Mountain, then accurate assessment of heat flow would require the accurate characterization of the highly heterogeneous distribution of fracture and matrix properties in the UZ. Because of the high degree of uncertainty concerning these heterogeneous distributions, as well as the highly nonlinear dependence of the flow system on these parameters, if UZ heat flow is dominated by heat convection, then validation of UZ heat flow models will be extremely formidable. On the other hand, if heat conduction dominates UZ heat flow, then the validation of UZ heat flow models will require accurate accounting of the thermal properties and thermal loading conditions. Incidentally, for the assessment of long-term, thermo-hydrological performance (i.e., \(t > 2000\) yr),
demonstrating that heat flow in the saturated zone (SZ) is also heat-conduction-dominated may be important. Thermal properties and thermal loading conditions can be more readily determined and are much less variable than many of the parameters associated with the ambient hydrogeological system. Moreover, the thermal properties are relatively insensitive to hydrothermally driven geochemical effects. The range of RIB values of thermal conductivity in the UZ at Yucca Mountain only spans a factor of 2.14. Consequently, of all the types of process models that may be required to support total systems performance assessment, models of heat-conduction-dominated heat flow will be, by far, the most readily validated.

We investigated the ability of the in situ heater tests to resolve the hypotheses concerning the dominant mode of heat flow. We also evaluated the time required for a definitive temperature signature to evolve during the course of the heater test. We compared the temperature profiles that are associated with heat-conduction-dominated heat flow with those associated with heat-convection-influenced heat flow. We use the term heat-convection-influenced (rather than heat-convection-dominated) because we find that even if large-scale, buoyancy-driven, gas-phase flow completely dominates the local boiling pressure gradients, thereby dominating the direction of steam flow within the boiling zone, the predominant mode of heat flow is still heat conduction. Two general conditions are required for heat-convection-influenced heat flow: (1) thermal loading conditions that are sufficiently high to drive large-scale, buoyancy-driven, gas-phase flow in conjunction with (2) sufficiently large bulk permeability, $k_b$, of the fractured rock mass. Because the horizontal scale of the repository is large relative to the thickness of the UZ, in order for large-scale, buoyancy-driven, gas-phase flow to affect repository-heat-driven hydrothermal flow, fracture pathways that give rise to the large $k_b$ must be contiguous throughout the UZ.

In our previous modeling study of repository-heat-driven hydrothermal flow, we found that for $k_b > 10^{-14}$m$^2$, the dry-out rate is effectively not throttled by flow resistance in the fractures. For $k_b > 10^{-11}$m$^2$, it was also found that heat convection, resulting from large-scale (i.e., far-field), buoyancy-driven, gas-phase flow, begins to influence repository-driven heat flow. Here, for the reference test configuration of 21 5.5-kW heaters (316.16 kW/acre over a 0.364-acre heated area), we considered a wide range in $k_b$, including a low-$k_b$ case with no fractures ($k_b = 1.9 \times 10^{-18}$m$^2$), the reference-$k_b$ case ($k_b = 2.8 \times 10^{-13}$m$^2$), and a high-$k_b$ case that is equivalent to having one 1000-μm fracture per meter ($k_b = 8.3 \times 10^{-11}$m$^2$). We chose this range of $k_b$ because it essentially covers the entire range of potential hydrothermal flow effects, including the low-$k_b$ case (associated with negligible boiling and convective flow effects), and the high-$k_b$ case, for which buoyancy-driven convection dominates the direction of steam flow in the boiling zone, thereby maximizing the impact of heat convection on heat flow within the boiling zone.

In this study, we found that it is necessary for large-scale, buoyancy-driven, gas-phase, pressure gradients to dominate the local boiling pressure gradients in order for heat convection to have a noticeable effect on overall heat flow. When local boiling pressure gradients, which drive steam away from the boiling zone, are more dominant than large-scale, buoyancy-driven, gas-phase pressure gradients, the flow of steam is vertically symmetrical about the heated horizon. The reference-$k_b$ case ($k_b = 2.8 \times 10^{-13}$m$^2$ in Figs. 9a–b and 10a–b) is an example of cases in which the local boiling pressure gradients dominate the large-scale, buoyancy-driven, gas-phase gradients. Notice that the temperature profile is vertically symmetrical about the heater horizon, indicating the symmetry of heat flow. Because the boiling pressure gradients are vertically symmetrical about the heated zone, roughly equal quantities of steam are driven above and below the dry-out zone, resulting in a vertically symmetrical distribution of condensate generation.

Superimposed on this vertically symmetrical distribution of condensate generation is the effect of condensate shedding off the sides of the boiling zone. Because of the relatively small size of the heater tests, we found that condensate shedding quickly results in a greater proportion of the condensate lying below the boiling zone for the reference-$k_b$ case, causing an apparent asymmetry in condensate distribution (Fig. 10b–c). However, because heat flow continues to be heat-conduction-dominated, the temperature distribution is vertically symmetrical about the heater horizon. Because the boiling rate is not throttled for $k_b > 10^{-14}$m$^2$, increasing $k_b$ above this threshold does not increase the rate at which steam is being generated, but it does increase the tendency for large-scale (i.e., far-field) buoyancy-driven convection cells to develop.
In analyzing the results from our previous study of repository-heat-driven hydrothermal flow,\textsuperscript{2} we found that $k_b = 8.3 \times 10^{-11} \text{m}^2$ is sufficiently large to allow far-field, gas-phase convection to drive 100\% of the steam in the condensation zone. Similarly, for the heater test calculations conducted in this study, $k_b = 8.3 \times 10^{-11} \text{m}^2$ was found to be sufficiently large to allow large-scale, buoyancy-driven, gas-phase flow to drive 100\% of the steam in the condensation zone. Although far-field convection completely dominated the direction of steam flow in the high-$k_b$ case, heat flow was still dominated by heat conduction, and the duration of the boiling period, $t_{bp}$, for the heater test decreased by only 11\% relative to the reference-$k_b$ case (Fig. 11a). The effect of heat convection was to enhance heat flow away from the boiling zone (relative to cases where heat-conduction completely dominated heat flow). Consequently, $T_{peak}$ was reduced from 214.1 to 194.2°C and $t_{bp}$ was reduced from 6.5 to 5.8 yr. In general, for $10^{-14} < k_b < 10^{-11} \text{m}^2$, given sufficiently high thermal loading conditions, significant dry-out occurs (unaffected by either flow resistance in the fractures or large-scale, buoyancy-driven, gas-phase flow), and thermal performance is relatively insensitive to $k_b$.

When far-field heat convection significantly influences far-field heat flow, large-scale, buoyancy-driven, gas-phase pressure gradients dominate the local boiling pressure gradients. Therefore, although local boiling pressure gradients in the lower dry-out zone tend to drive steam downward, large-scale convection cells dominate steam flow, resulting in steam being driven upward. Consequently, in the high-$k_b$ case, all of the steam is driven upward, where it condenses above the dry-out zone (Fig. 10a–c). Therefore, for convection-influenced heat flow, we would expect to see nearly all of the condensation occurring above the dry-out zone. This asymmetry in condensate distribution results in a much larger vertical upward component of latent heat convection in the high-$k_b$ case, manifested by very pronounced vertical asymmetry in the temperature distribution (Fig. 9a–c) and reduced temperatures near the heaters (Fig. 11a). The vertical extent of the upper heat-pipe (or condensate) zone in the high-$k_b$ case is more than three times that of the reference-$k_b$ case. The resultant vertical upward shift in the upper boiling temperature front is 2.5 m after 1 yr, 5.5 m after 2 yr, and 9.0 m after 4 yr of full-power heating in the 21 5.5-kW heater test configuration (Fig. 9a–b). Therefore, the vertical temperature distribution is a very clear discriminator as to whether far-field convection significantly affects heat flow. Based on the results for the reference 21-heater test configuration, it appears that after 2 years of full power heating with 5.5 kW heaters, we may have adequate data to begin to validate the basic hypothesis test that heat conduction dominates repository-driven UZ heat flow. Diagnostic thermal probes, which are extremely sensitive to differences between conductive and convective heat flow, would also assist in the interpretation of convective effects.\textsuperscript{21}

As has been covered in our previous study of repository-heat-driven hydrothermal flow,\textsuperscript{2} contrary to the assumption that the $k_b$ distribution is isotropic and homogeneous, actual fracture distributions show significant variability. In one study, water dripping from fractures was collected along the ceiling of a tunnel complex at Stripa, Sweden.\textsuperscript{24} It was found that 50\% of the mass flux occurred from 3\% of the flow area and that very little flow occurs over 70\% of the flow area. It is not certain to what extent this variability in mass flux was due to stresses induced during the mining of the drifts. Generally, a large $k_b$ measurement is the result of flow channeling along a few highly transmissive fracture pathways. Although we assign a homogeneous, isotropic value of $k_b$ in using the ECM, the local value of $k_b$ is probably orders of magnitude smaller than the bulk-averaged value for much of the fractured rock mass.

As mentioned earlier, we also considered the low-$k_b$ case with no fractures ($k_b = 1.9 \times 10^{-18} \text{m}^2$). When no fractures are present, there is no significant boiling because the small matrix permeability results in very large gas-phase pressures, causing a rise in the boiling temperature, $\Delta T_b$. In the analysis of the G-Tunnel heater test,\textsuperscript{17} the gas-phase pressure, $p_g$, gradients in large matrix blocks (fracture spacing of 3 m) resulted in $p_g$ being tens of atmospheres above ambient, driving large $\Delta T_b$,s and thereby suppressing boiling. When no fractures are present, the small matrix permeability results in negligible gas-phase flow. The absence of convective and boiling effects results in heat-conduction-dominated heat flow and higher $T_{peak}$ than the case with significant convective flow in fractures and boiling effects (Fig. 11a). For 4 years of full-power heating, the duration of boiling, $t_{bp}$, is 6.5 and 7.0 yr for the reference-$k_b$ case ($k_b = 2.8 \times 10^{-13} \text{m}^2$) and the low-$k_b$ case, respectively (Fig. 11a).

Incidentally, in the studies of repository-heat-driven hydrothermal flow,\textsuperscript{7,8} we saw a negligible difference in $t_{bp}$ between the low-$k_b$ case with no fractures and the reference-$k_b$ case. Although during the course of the heater tests the range in potential thermal performance is relatively narrow, it appears...
that differences in thermal performance that arise due to either the absence or presence of significant boiling and convective effects may be more apparent at the scale of the heater tests than will be manifested at the repository scale.

For 4 years of full-power heating, $t_{\text{peak}}$ is 214.1 and 221.4°C for the reference-$k_b$ case ($k_b=2.8 \times 10^{-13} \text{m}^2$) and the low-$k_b$ case, respectively (Fig. 11a). For the low-$k_b$ case, the maximum gas-phase pressure ($p_g=23.56 \text{ atm}$) at the center of the heater test occurs at the same time that the temperature peaks ($t_{\text{peak}}=4 \text{ yr}$). We find in the steam tables that the saturation temperature (equivalent to the boiling temperature) corresponding to $p_g=22.56 \text{ atm}$ is 220.9°C. Because of the vapor-pressure-lowering effect due to capillarity, the actual boiling temperature (221.4°C) is slightly higher than the saturation temperature in the steam tables. As was observed for the heat pipe effect in the reference-$k_b$ and high-$k_b$ cases (Fig. 9a–c), boiling has a pronounced effect on flattening the temperature profile around the nominal boiling temperature, $T_b$. Because of the large $k_b$ in the reference-$k_b$ and high-$k_b$ cases, $p_g$ does not increase significantly above the ambient pressure of 0.896 atm at the heater horizon, and the actual boiling temperature coincides with the nominal boiling point ($T_b=96^\circ \text{C}$). In general, until heating can drive the liquid saturation to zero, temperatures will be determined by two-phase thermodynamic equilibrium. In the low-$k_b$ case, high $p_g$ cause the actual boiling temperature to be much greater than $T_b$. Because of the very low $k_b$ in the case with no fractures, the thermal load is insufficient to drive water vapor away, thereby lowering the saturation to zero; therefore, temperatures continue to be determined by two-phase thermodynamic equilibrium.

Cooldown and Re-Wetting Performance

The validation of thermo-hydrological performance models will be greatly facilitated by several hypothesis tests, the last of which concerns the question of whether re-wetting of the dry-out zone back to ambient saturation lags significantly behind the end of the boiling period. In this sub-section, we investigate the ability of the in situ heater tests to resolve this question.

For the reference 21 5.5-kW heater test configuration, we modeled a linear ramp-down from full-power at $t=4 \text{ yr}$ to zero power at $t=5 \text{ yr}$ for the low-$k_b$ case, the reference-$k_b$ case, and the high-$k_b$ case. Although heating stopped at $t=5 \text{ yr}$, the duration of boiling, $t_b$, was 6.5 yr at the center of the heater array for the reference-$k_b$ case (Fig. 11a). The effect of far-field convection modestly enhancing heat flow from the dry-out zone resulted in an 11% reduction in $t_b$ (relative to the reference-$k_b$ case) for the high-$k_b$ case ($t_b=5.8 \text{ yr}$). The absence of any convective effects for the low-$k_b$ case with no fractures resulted in $t_b=7 \text{ yr}$.

Because the thermal diffusivity of most of the hydrostratigraphic units in the UZ is much larger than the re-wetting diffusivity, temperature tends to equilibrate back to ambient conditions much more quickly than does saturation. Similarly, for the heater test, re-wetting of the heater horizon back to ambient saturation takes much longer than cooling to sub-boiling temperatures (Fig. 11a–b). or the reference-$k_b$ case, it takes 533 yr for the center of the heater array to re-wet back to one-half ambient saturation (34.35%). Although the high-$k_b$ case dried out 13% less rock than the reference-$k_b$ case, it takes 16.5% longer (621 yr) to re-wet back to one-half ambient saturation. The longer re-wetting time is due to the enhanced vertical component of gas-phase flow transporting moisture away from the condensate zone above the dry-out zone. The saturation buildup above the dry-out zone (evident in Fig. 12c) is more rapidly dissipated by buoyancy-driven vapor flow in the high-$k_b$ case; consequently, there is less liquid saturation in the vicinity of the dry-out zone to re-wet the dry-out zone. Both the reference-$k_b$ and high-$k_b$ cases require nearly 5000 yr to re-wet back to ambient saturation.

Details of the boiling, dry-out, and re-wetting performance at the center of the reference-test configuration during the cooldown period are examined for the reference-$k_b$ case by plotting the vertical liquid saturation, $S_l$, and temperature profiles (Fig. 12a–c). The temperature distribution is shown as the heavy solid line, and the nominal boiling temperature, $T_b$, is shown as the light dotted line. Notice that the initial (ambient) $S_l$ distribution (shown as the light solid line), corresponds to a net recharge flux of 0 mm/yr (Fig. 1). The $S_l$ distribution for nonzero times is plotted as the dashed line; the dark shaded areas correspond to $S_l$ wetter than ambient saturation (condensate zones), and the light shaded areas correspond to $S_l$ drier than ambient saturation (dry-out zone). Notice that the flattening of the temperature profile coincides with the zone of condensate buildup (Fig. 12a). The flattening of the temperature profile above the dry-out zone is primarily attributed to the gravity-driven refluxing, or heat pipe effect,
while the flattening of the temperature profile below the heater test is attributed to condensate drainage convecting heat away from the boiling zone.

Figure 12a is plotted at the time when the center of the heater test reaches its peak temperature ($t_{\text{peak}} = 4$ yr). This is also when the vertical extent of the nominal boiling front reaches its maximum, with $T \geq T_b$ from $z = 332.22$ to $353.69$ m. For this report, we define the dry-out front as corresponding to the location where the normalized liquid saturation $S_1 = 0.5$, where $S_1$ is defined as

$$\bar{S}_1 = \frac{S_1}{S_{\text{limit}}}$$

where $S_{\text{limit}}$ is the initial liquid saturation. In other words, the dry-out front occurs where $S_1$ is 50% percent drier than ambient. At $t = 4$ yr, the vertical extent of the dry-out zone is 14.26 m, extending from $z = 336.44$ to 350.70 m. Figure 12b is plotted just as the boiling period ends at the center of the heater test. Although temperatures have cooled below boiling, the saturation buildup persists in the condensation zones (Fig. 12c).

Nitao observed that for $S_1 < 10\%$, re-wetting of the dry-out zone is dominated by vapor flow in fractures and capillary condensation along fractures. For the high-$k_b$ case, a very rapid rise in $S_1$ is evident at the heater horizon for $6 < t < 20$ yr. The high $k_b$ facilitates rapid gas-phase re-wetting of the dry-out zone for $0 < S_1 < 10\%$. For $S_1 > 10\%$, vapor flow no longer contributes significantly to re-wetting; therefore, subsequent buildup of saturation at the heater horizon must await the slow process of matrix imbibition, which occurs at the edge of the dry-out front (Fig. 11a–b). In comparing $t = 4, 6.5, 8$ yr, and the movement of the re-wetting front is hardly perceptible (Fig. 12a–b). The slow matrix-imbibition-controlled re-wetting of the dry-out zone is also manifested by a plateau in the re-wetting curve (Fig. 11b). Incidentally, notice that $k_b$ in the reference-$k_b$ case is not sufficiently large to facilitate the rapid early buildup in $S_1$; therefore, $S_1$ remains close to zero throughout the first 20 yr.

Analysis of Heater Test Size

We investigated the impact of test size by considering three cases: (1) 6 3.53-kW heaters in two parallel heater drifts with 8.23-m center-to-center drift spacing, (2) 21 5.5-kW heaters in three parallel heater drifts with 12.8-m center-to-center drift spacing, and (3) 65 6.11-kW heaters in five heater drifts with 14.26-m center-to-center drift spacing. These dimensions were chosen in conjunction with the 5.49-m center-to-center spacing between heaters in order for the heater configuration to cover a square heated area. For the 6-heater test, the heated area is 16.46 x 16.46-m; for the 21-heater test, the heated area is 38.4 x 38.4-m; and for the 65-heater test, the heated area is 71.3 x 71.3-m. We represented the square heated areas with disks having the same area and same averaged heat load per unit area as the test configuration. The heater power for the respective cases was chosen to yield the same averaged heat load per unit area.

At the center of the heater array, the rock temperatures at the end of 4 yr of full-power heating are 147, 214, and 231°C for 6, 21, and 65 heaters, respectively (Fig. 13), while the vertical thickness of the dry-out zone is 5.6, 14.3, and 16.6 m for these three test configurations. While the differences between the 21-heater and 6-heater arrays are substantial, the differences between the 21-heater and 65-heater tests are minor. Effectively, the center of the 21-heater array performs as though the test were infinite in areal extent during the first 4 yr of full-power heating.

Concerns about the scale of hydrogeological heterogeneity aside, the center of the 21-heater test performs almost as effectively as that of the 65-heater test. Therefore, the 21-heater test is the minimum recommended size for 4 yr of full-power heating. Recall that fracture flow measurements made at Stripa were thought to be affected by excavation closing fractures in the vicinity of the drifts. The rule of thumb is that stresses induced during the mining of drifts affect the rock mass in a region of up to 2 drift diameters into the rock (starting at the drift walls). Mining-induced rock stresses can result in either closing or opening of fractures, thereby changing the bulk permeability, $k_b$, and possibly the connectivity of fractures in the affected regions.

It should be noted that steep temperature gradients in the vicinity of the heater drifts will also induce stress changes that may open or close fractures. Figures 9, 10, and 12 indicate that the steep temperature gradients are confined to the dry-out zone. Therefore, it should follow that thermally-induced stress effects (as with the mining-induced stress effects) are confined to a region relatively
close to the heat source, i.e., either the WPs or heaters.

For thermo-hydrological performance, the principal concerns are the impact of the geomechanically altered \( k_b \) and fracture connectivity on boiling and condensate drainage as well as on large-scale, buoyancy-driven convection. Recall that our analyses\(^7\) indicated that, for \( k_b > 10^{-14} \text{ m}^2 \), flow resistance in fractures does not throttle the rate of boiling and dry-out. We also found that for \( k_b > 10^{-11} \text{ m}^2 \), large-scale, buoyancy-driven, gas-phase convection cells begin to dominate local boiling pressure gradients, thereby dominating the direction of steam flow within the boiling zone. We find that it is necessary for large-scale, buoyancy-driven gas-phase flow to dominate the direction of steam in order for heat convection to influence overall heat flow. Recall also that we found that for fracture apertures greater than 20 \( \mu \text{m} \), flow resistance in the fracture does not throttle the rate of boiling and dry-out.\(^17\)

The \( k_b \) data applicable to the UZ is limited. Montazer and coworkers\(^27\) measured bulk (air) permeability, \( k_b \), in the TSw to a depth of 100 m, obtaining \( 7 \times 10^{-13} < k_b < 1 \times 10^{-11} \text{ m}^2 \). For well J-13, Thordarson\(^28\) reported a bulk hydraulic conductivity of 1 m/day (\( k_b = 1.7 \times 10^{-11} \text{ m}^2 \)) for TSw2. On the basis of the available data, it appears that \( k_b \) is substantially higher than the threshold \( k_b (10^{-14} \text{ m}^2) \) for significant boiling and dry-out. Therefore, it is not likely that geomechanical effects could reduce \( k_b \) enough to throttle the rate of boiling and dry-out. If significant geomechanical alteration of the hydrological properties does occur, it is more likely to affect large-scale, buoyancy-driven, gas-phase flow.

For actual repository conditions, the impact of mining-induced and thermal stresses on the fracture properties in the vicinity of the drifts is relevant to repository-heat-driven hydrothermal flow for roughly the first 100 yr. For \( t > 100 \text{ yr} \), the region of rock affected by boiling, condensate flow, and far-field convection extends well beyond the region of mining-induced stress effects. Incidentally, thermally-induced stress changes will occur farther out into the rock than mining-induced stress changes. However, the region where thermally-induced stress change may result in significant alteration of hydrological properties is likely to be concentrated in the vicinity of the emplacement drifts, where temperature gradients are steepest and the cumulative impact of stress change is greatest. Consequently, the region of significant thermally-induced stress change may be approximately coincident with the region of mining-induced stress effects. Therefore, the effects of mining-induced stress (and perhaps thermally-induced stress) during the course of a relatively short-term, in situ heater test would not be relevant to much of the UZ that impacts long-term, repository-heat-driven, hydrothermal flow.

If mining-induced stress effects are found to critically affect boiling, dry-out, condensate flow, and re-wetting performance, it will be necessary to conduct the in situ heater tests long enough for the dry-out zone (and condensate shedding zones) to extend well beyond the region affected by mining-induced stresses. If we assume that mining-induced stresses have significantly altered \( k_b \) in a region extending 2 drift diameters into the rock, then the total area affected by mining-induced stress extends 5 drift diameters vertically and 5 drift diameters horizontally. A 4.6 x 4.6 m drift would affect a 23 x 23 m area of rock. If we were to reduce the heater drift size to 3 x 3 m, the affected area would be 15 x 15 m. For the reference 21-test configuration (with 12.8 m center-to-center drift spacing), the entire pillar area would be affected. Therefore, it may be necessary to increase the drift spacing, and thereby increase the time required to coalesce the dry-out zones between the drifts.

**Analysis of Heater Test Duration**

Recall that for determining the size and duration of the heater tests, we considered the following criteria:

1. the velocity of the dry-out front,
2. the size and duration of condensate perching,
3. the peak rock temperatures,
4. the time rate of change of temperature, and
5. the volume of the dry-out zone.

Based on the discussion in the previous sub-section, we have already selected the heater test size to be that of the reference-test configuration (21 heaters in three heater drifts). For the 21-heater test configuration, we considered heating rates from 2.25 to 22 kW per heater (Table 1). With respect to the five hydrothermal flow criteria listed above, we have summarized the thermo-hydrological
performance of the six heating rates (Figs. 14, 15a-b, and 16a-b). We have also plotted relevant thermo-hydrological performance measures for the 30-yr-old Spent Nuclear Fuel (SNF) and Areal Power Densities (APDs) of 57 and 114 kW/acre (Fig. 17a–b). 8

Criterion (1) is addressed by comparing Fig. 16b and 17b. Criterion (4) is addressed by comparing Fig. 16a and 17a. Criterion (2) has already been partly addressed in the analysis of heater test size; however, Fig. 16b also relates to criterion (2).

Notice that criteria (3) and (5) can be addressed by using Fig. 15a and 15b in tandem. On the basis of a desired vertical dry-out zone thickness, \( h_{dz} \), and desired peak rock temperature, \( T_{peak} \), we can determine the required heater rate and required full-power heating duration by the sequential use of Fig. 15b and 15a. The first step is to find the intersection of the desired \( h_{dz} \) and \( T_{peak} \) in Fig. 15b. On the basis of this intersection, the required heater power is obtained by interpolating between the 6 heater power curves in Fig. 15b. Using Fig. 15a, the value of \( t \) where the desired \( h_{dz} \) intersects the required heater power curve is the required time of full-power heating.

We start with the fifth criterion. On the basis of Figs. 4, 5, 7, and 8 and the discussion on the detailed boiling, dry-out, and condensate flow behavior, we decided that adequate coalescence of the dry-out zones occurs when dry-out extends approximately 14 m vertically. For 5.5-kW heaters, 14.26 m of vertical dry-out is achieved in 4 yr (Fig. 15a) with a \( T_{peak} \) of 214°C (Fig. 14). For 22-kW heaters, 14.26 m of vertical dry-out is achieved in 14 yr (Fig. 14), 3.4, 2.54, 1.7, and 1.07 yr, respectively, with \( T_{peak} \) of 230, 264, 335, and 485°C, respectively (Figs. 14 and 15a, and Table 1). Therefore, in order to achieve coalescence between heater drifts during a 1.5-yr test, it is necessary to drive \( T_{peak} \) to nearly 500°C. Moreover, the velocity of the dry-out front (Fig. 16b) associated with the 22-kW accelerated-rate test is on the order of 10 m/yr, which is much greater than the dry-out front velocities (≤1.0 m/yr) typical of repository conditions (Fig. 17b). 7, 8

We base our decision on the third criterion primarily on the concern about exceeding the mineral-phase transformation temperature for cristobalite (about 200 to 240°C). We find that \( T_{peak} \) for the 5.5-kW heater test (214°C) does not substantially violate the lower end of this temperature range. In Fig. 8, we see that 200°C is only exceeded for the first 2 m of rock adjacent to the central heater drift.

The first and second criteria primarily relate to thermal-geochemical coupling at the refluxing front, which may result in geochemical alteration of fracture and matrix properties. If the dry-out front is driven too quickly, there will be inadequate time for geochemical effects to occur. We feel that it is necessary to maintain two-phase flow conditions in the fractures overlying the boiling zone for at least 1 yr. Based on the observations that the upper two-phase heat-pipe zone is approximately 3 to 5 m in thickness for \( 2 < \frac{dT}{dt} < 4 \) °C/yr (Fig. 10b and c), and that the velocity of the upper dry-out front is approximately 1.5 to 2 m/yr over the same time frame (Fig. 16b), the minimum residence time for two-phase conditions in the upper refluxing zone is 2 yr for the 5.5-kW heater test. Note that the upper dry-out front velocity is approximately half the rate of increase of the vertical dry-out zone thickness. Therefore, the 5.5-kW heater test should provide sufficient time for geochemical effects in the upper refluxing zone to be manifested.

The third and fourth criteria relate to the potential of geomechanical and geochemical effects significantly altering the thermo-hydrological properties in a way that is not representative of repository thermal loading conditions. We have already addressed the third criterion. With respect to the fourth criterion, for 30-yr-old SNF and an APD of 114 kW/acre, the maximum rate of temperature rise, \( \frac{dT}{dt} \), is approximately 25°C/yr (Fig. 17a). Notice that within 6 yr, \( \frac{dT}{dt} \) decreases to about 5°C/yr. For 5.5-kW heaters, the initial \( \frac{dT}{dt} \) is about 50°C/yr, declining to about 20°C/yr after 4 yr. Therefore, it appears that the 5.5-kW heater test meets the fourth criterion.

Because of the potential impacts of hydrogeologic heterogeneity on dry-out performance, it is most critical that we not violate the fifth criterion. In order to meet the fifth criterion with a highly accelerated 1.5-yr heater test, the 22.0-kW heater test is required. The 22.0-kW heater test results in \( T_{peak} \) of 485°C, and the first, second, and fourth criteria are also seriously violated. If we relax the requirement of 14.26 m of vertical dry-out thickness to just 12.73 m and only require that full-power heating be accomplished in 1.5 yr, then it is possible to meet the fifth criterion with 12.38-kW heaters. The 12.38-kW test results in a \( T_{peak} \) of 335°C, and serious questions concerning the first, second, and fourth criteria remain.
CONCLUSIONS

This study examined the hydrothermal performance of in situ heater tests in order to address the issue of whether the current schedule for the Exploratory Studies Facility (ESF) at Yucca Mountain allows for adequate heater test duration prior to the 2001 License Application Deadline (LAD), or whether an earlier start date is required. The success of the License Application (LA) hinges largely on how effectively we validate the process models that provide the basis for performance assessment.

The validation of thermo-hydrological models for a high-AML repository, which generates long-term boiling conditions with significant dry-out, will be greatly facilitated by addressing several fundamental hypothesis tests: (1) whether heat conduction dominates heat flow, (2) whether fracture density and connectivity are sufficient to promote rock dry-out due to boiling and condensate shedding, and (3) whether re-wetting of the dry-out zone (and repository horizon) back to ambient saturation significantly lags behind the end of the boiling period. In addressing these hypotheses, we will ask whether hydrothermal-geochemical-geomechanical coupling must be dynamically accounted for in performance models or whether it can be conservatively accounted for by bounding analysis. A major objective of this study was to evaluate the ability of the in situ heater tests to answer these fundamental hypothesis tests. We examined how, during the course of highly accelerated and nominal-rate in situ heater tests, the information obtained will progressively resolve these critical questions.

The validation of thermo-hydrological models for a low- to medium-AML repository, which generates sub- or marginal-boiling conditions with insignificant dry-out, will be critically dependent on characterizing two factors: the highly heterogeneous distribution of fracture and matrix properties, and the highly spatially and temporally variable distribution of net recharge flux. Validation will require demonstrating that the models can adequately account for (1) these very complex, variable distributions, (2) the strongly nonlinear dependence of fracture flow on these heterogeneous distributions, (3) how the spatial variability in the heat-generation rate among the WPs will drive condensate flow from hotter to cooler WPs (and possibly to the water table), and (4) the impact of hydrothermal-geochemical coupling on the flow and transport properties as well as on the chemistry of water contacting WPs. Because sub-boiling or marginal-boiling conditions will not mitigate the occurrence of deep nonequilibrium fracture flow (from meteoric sources), hydrologic assessments of low thermal loading conditions must account for the superposition of naturally occurring episodic fracture flow and repository-heat-driven refluxing for essentially the entire time of regulatory concern. The in situ heater tests will provide information that is particularly critical to issues (3) and (4); however, examining detailed behavior of condensate shedding will also provide critical insight related to items (1) and (2). These tests will also help us determine how these issues impact WP integrity and radionuclide transport.

Hydrothermal calculations of in situ heater tests were conducted with the use of the V-TOUGH code and the Equivalent Continuum Model for a range of (1) test sizes (number of heaters and heater drifts), (2) heating rates, and (3) bulk permeabilities, \( k_b \). The in situ heater tests are represented with two kinds of models. The drift-scale model is a two-dimensional cross-sectional model that explicitly represents the details of the heaters and heater drifts in the plane orthogonal to the drift axes. Because it is two-dimensional, the drift-scale model assumes that the heater drifts are infinitely long, effectively neglecting the heat loss that occurs due to heat flow parallel to the drift axes. We also use an R–Z ("test-scale") model that represents the heater test as a disk-shaped uniform heat source with a radius that corresponds to the size of the test configuration. The R–Z test-scale model smears temperature effects between the heater drifts at very early time, but it compares well with the cross-sectional drift-scale model after the dry-out zones have coalesced. It also has the advantage of accurately accounting for the overall heat flow. With the R–Z test-scale model, we represented the reference 21-heater, 3-drift test (0.364-acre heated area), a 6-heater, 2-drift test (0.067-acre heated area), and a 65-heater, 5-drift test (1.256-acre heated area).

The reference-test case consists of 21 5.5 kW heaters. A 4-yr full-power heating period is followed by a 1-yr linear ramp-down from full- to zero-power. For the reference-test case, coalescence of the dry-out zones between the drifts takes about 3 yr. Condensate dripping from the ceiling of the heater drifts is also likely to persist for at least 3 yr. Coalescence of the dry-out zones effectively results in hydrothermal perching of the condensate over a boiling region of approximately 0.364 acres for at least the final year of the full-power heating period (3 < t < 4 yr). Because the duration of the boiling period in the center of the test is 6.5 yr, the perching of condensate will persist even after the
heater power has been ramped to zero.

Thermal interference from the heaters in the outer drifts was observed to facilitate rapid dry-out in the center of the test without the need to substantially overdrive the temperatures around the central drift. Had we chosen to heat with just one heater drift, it would have required a much greater heater output (per heater) to achieve the same rate of dry-out, thereby resulting in a steeper temperature gradient in the dry-out zone. The use of multiple heater drifts allows a given rate of dry-out to be achieved at lower peak temperatures. Therefore, we find that it is critical for the heater test configuration to include at least three heater drifts. Concerns about the scale of hydrogeologic heterogeneity aside, the center of the 21-heater, 3-drift test performs almost as effectively as that of the 65-heater, 5-drift test. For the first 4 yr, the thermal performance at the center of the 21-heater test was very similar to that of the 65-heater test. Effectively, cooling at the outer edge of the heater test does not affect the thermal performance at the center of the 21-heater test for the first 4 yr. Therefore, the 21-heater, 3-drift test is the minimum recommended size for 4 yr of full-power heating. If a longer full-power heating duration were required, edge-cooling effects would necessitate going to a larger test configuration. There are additional factors affecting test size. Because of the potential for significant alteration of the fracture properties by mining-induced stresses, it may be necessary to increase the test size and duration in order to drive the dry-out and condensation zones well beyond the region that is significantly affected by excavation.

In examining the sensitivity of heater test performance to $k_b$, we find that there are three distinct categories of thermo-hydrological performance. The low-$k_b$ case ($k_b < 10^{-14} \text{m}^2$) corresponds to a situation in which fracture density and connectivity throttle the rate of boiling and dry-out. Because heat flow is dominated by heat conduction, it is vertically symmetrical about the heater horizon. The low $k_b$ results in large gas-phase pressure gradients that elevate the boiling temperature, thereby resulting in higher peak temperatures. The intermediate-$k_b$ case ($10^{-14} < k_b < 10^{-11} \text{m}^2$) corresponds to a situation in which the fracture density and connectivity are sufficient to promote boiling that is no longer throttled by flow resistance in the fractures. Because $k_b$ is not sufficiently large to promote substantial large-scale buoyancy-driven convection, local boiling pressure gradients dominate the large-scale, buoyancy-driven, gas-phase pressure gradients, resulting in steam flow and condensate generation that is vertically symmetrical about the heater horizon; consequently, heat flow continues to be symmetrical about the heater horizon. The heat convective effects in the heat-pipe zone have a local, transient effect on the temperature distribution, but because convection does not significantly enhance the heat loss from the boiling zone to the far-field, the duration of boiling conditions is not significantly reduced.

The high-$k_b$ case ($k_b > 10^{-11} \text{m}^2$) corresponds to a situation in which fracture density and connectivity are sufficiently large to allow large-scale, buoyancy-driven gas-phase gradients to dominate the local boiling pressure gradients, causing significant asymmetry in the vertical temperature distribution. For this category of thermo-hydrological performance, we considered an extreme example ($k_b = 8.3 \times 10^{-11} \text{m}^2$). Although far-field convective flow was observed to completely dominate the direction of steam flow, causing 100% of the steam to be driven to the upper condensation zone, heat flow was still dominated by heat conduction, and the duration of the boiling period, $t_b$, for the heater test decreased by only 11% relative to the intermediate-$k_b$ case. Because large-scale convection enhances the heat loss from the boiling to the far-field, heat convection has a definite influence on heat flow (e.g., lowering peak temperatures), yet it does not dominate overall heat flow and boiling performance. Although the high-$k_b$ case dried out 13% less rock than the intermediate-$k_b$ case, it takes 16.5% longer (621 yr) to re-wet back to one-half ambient saturation (the intermediate-$k_b$ case requires 533 yr). The longer re-wetting time is due to the enhanced vertical component of gas-phase flow transporting moisture away from the condensate zone above the dry-out zone.

Although the duration of boiling does not vary substantially among the low-, intermediate, and high-$k_b$ cases, their vertical temperature profiles were very distinctive. It appears that after 2 yr of full-power heating with 5.5-kW heaters, temperature measurements can be used to validate the basic hypothesis test concerning the question of whether repository-driven UZ heat flow is dominated by heat conduction.

Relative to repository conditions, in situ tests will be accelerated during both the heating and cooling cycles. For the reference 21-heater test, we systematically analyzed the required heater duration on the basis of five criteria, including (1) the velocity of the dry-out front, (2) the size and duration of
condensate perching, (3) the peak rock temperatures, (4) the time rate of change of temperature, and (5) the volume of the dry-out zone. For in situ heater tests to be applicable to actual repository conditions, a minimum heater test duration of 6-7 yr (including 4 yr of full-power heating) was determined. We also evaluated trade-offs associated with more highly accelerated tests. The parallel use of highly accelerated, shorter-duration tests may provide timely information for the LA, provided that the applicability of the test results can be validated against ongoing nominal-rate heater tests.

The three fundamental hypothesis tests presented in this paper can greatly focus the critical characterization, modeling, laboratory, and in situ testing activities required in building robust site suitability and licensing arguments. The validation of these hypotheses will profoundly reduce the impact of hydrogeological uncertainty and variability on the predictability of total system performance. The most conclusive means of testing these hypotheses involve large-scale in situ heater tests at various hydrostratigraphic intervals of the UZ. In situ heater tests will also be extremely useful in determining whether temperature rise, condensate flow, and buoyancy-driven SZ flow can drive geochemical changes that significantly alter properties within the engineered and natural barriers. Critical performance issues involving hydrothermal-geochemical-geomechanical coupling cannot be entirely resolved either in the laboratory or through modeling. Moreover, critical hydrological performance issues cannot be entirely resolved by ambient property measurements conducted during site characterization. Therefore, in situ heater tests at various hydrostratigraphic intervals (above as well as below the repository horizon) will be critical to addressing such issues.

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REFERENCES


Table 1. Dry-out times, peak drift wall temperature, minimum rate of temperature rise, and minimum vertical dry-out rates associated with vertically drying out 14.26 m of rock at the center of 3 heater drifts each containing 7 heaters.

<table>
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<th>Heater power (kW)</th>
<th>Dry-out time (yr)</th>
<th>Peak drift wall temperature (°C)</th>
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<th>Minimum vertical dry-out rate (m/yr)</th>
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Figure 1. ECM-calculated liquid saturation profile for various steady-state, one-dimensional recharge fluxes vs data from the Reference Information Base (RIB).
Figure 2. Schematic of hydrothermal flow near the emplacement drift. Rock dry-out occurs as boiling drives water vapor out of the rock matrix. Upon reaching the fracture network, vapor is driven away from the boiling zone to where cooler temperatures cause it to condense along fracture walls. Because the small $k_m$ limits the rate of matrix imbibition, condensate drainage persists for considerable distances down fractures.
Figure 3. A "hydrothermal umbrella" is established along each of the emplacement drifts because of condensate being shed off the top and down the sides of the boiling zone.
Figure 4. Dimensionless liquid saturation distribution orthogonal to an array of three infinitely-long, parallel heater drifts, each generating 1.0 kW/m of drift, at (a) $t = 1$ yr and (b) $t = 2$ yr. Medium shaded areas next to the heater drifts correspond to regions that are drier than ambient liquid saturation (dry-out zones). Dark shaded areas correspond to regions that are wetter than ambient liquid saturation (condensation zones). Note that the light shading surrounding the dark shaded area in (a) corresponds to a small rise in liquid saturation (outer edge of condensation zones). No shading indicates no change in liquid saturation.
Figure 5. Dimensionless liquid saturation distribution orthogonal to an array of three infinitely-long, parallel heater drifts, each generating 1.0 kW/m of drift, at (a) $t = 3$ yr and (b) $t = 4$ yr. Medium shaded areas next to the heater drifts correspond to regions that are drier than ambient liquid saturation (dry-out zones). Dark shaded areas correspond to regions that are wetter than ambient liquid saturation (condensation zones). No shading indicates no change in liquid saturation.
Figure 6. A comparison of thermal performance predicted by the cross-sectional model of three infinitely-long, parallel heater drifts, each generating 1.0 kW/m of drift, and the R-Z model of a disk-shaped, 1.5-m-thick, 0.364-acre heat source with a uniform heat load of 316.16 kW/acre. Temperature histories are plotted for the central drift wall at the mid-heater elevation and the center of a uniformly heated area representing 21 5.5-kW heaters.
Figure 7. A comparison of dry-out performance predicted by the cross-sectional model of three infinitely-long, parallel heater drifts, each generating 1.0 kW/m of drift, and the R-Z model of a disk-shaped, 1.5-m-thick, 0.364-acre heat source with a uniform heat load of 316.16 kW/acre. The history of vertical dry-out zone thickness is plotted for the centerline of the central heater drift, the mid-pillar centerline between heater drifts, and the centerline of a uniformly heated area representing 21 5.5-kW heaters.
Figure 8. Horizontal temperature (a) and liquid saturation (b) profile through the mid-heater depth, orthogonal to an array of three infinitely-long, parallel heater drifts, each generating 1.0 kW/m of drift. Note that the shaded area in (a) corresponds to the interior of the heater drift.
Figure 9. Vertical temperature profile along the centerline of the R-Z model of a disk-shaped, 1.5-m-thick, 0.364-acre heat source with a uniform heat load of 316.16 kW/acre, representing 21 5.5-kW heaters at (a) $t = 1$ yr, (b) $t = 2$ yr, and (c) $t = 4$ yr. The temperature profiles are plotted for the reference-$k_b$ case ($k_b = 2.8 \times 10^{-13} \text{m}^2$) and the high-$k_b$ case ($k_b = 8.3 \times 10^{-11} \text{m}^2$).
Figure 10. Vertical liquid saturation profile along the centerline of the R-Z model of a disk-shaped, 1.5-m-thick, 0.364-acre heat source with a uniform heat load of 316.16 kW/acre, representing 21 5.5-kW heaters at (a) t = 1 yr, (b) t = 2 yr, and (c) t = 4 yr. The temperature profiles are plotted for the reference-$k_b$ case ($k_b = 2.8 \times 10^{-13}\,\text{m}^2$) and the high-$k_b$ case ($k_b = 8.3 \times 10^{-11}\,\text{m}^2$).
Figure 11. Temperature history (a) and liquid saturation history (b) at the center of the heated area for the R-Z model of a disk-shaped, 1.5-m-thick, 0.364-acre heat source with a uniform heat load of 316.16 kW/acre, representing 21 5.5-kW heaters. The temperature and liquid saturation histories are plotted for the reference-$k_b$ case ($k_b = 2.8 \times 10^{-13}$ m$^2$) and the high-$k_b$ case ($k_b = 8.3 \times 10^{-11}$ m$^2$). The temperature history is also plotted for the low-$k_b$ case ($k_b = 1.9 \times 10^{-18}$ m$^2$).
Figure 12. Vertical temperature profile (heavy solid line) and liquid saturation profile (dashed line) along the centerline of the R-Z model of a disk-shaped, 1.5-m-thick, 0.364-acre heat source with a uniform heat load of 316.16 kW/acre, representing 21 5.5-kW heaters for the reference-\(k_b\) case \((k_b = 2.8 \times 10^{-13} \text{m}^2)\) at (a) \(t = 4\) yr, (b) \(t = 6.5\) yr, and (c) \(t = 8\) yr. The medium solid line is the initial liquid saturation distribution. The light shaded area indicates depths that are drier than ambient saturation (dry-out zone). The dark shaded areas indicate depths that are wetter than ambient saturation (condensation zones). The nominal boiling temperature \((T_b = 96^\circ\text{C})\) is plotted as the light dotted line.
Figure 13. Temperature history at the center of the heated area for the R-Z model of a disk-shaped, 1.5-m-thick heat source with a uniform heat load of 316.16 kW/acre for a 1.256-acre heated area (representing 65 6.11-kW heaters), a 0.364-acre heated area (21 5.5-kW heaters), and 0.067-acre heated area (6 3.53-kW heaters).
Figure 14. Temperature history at the center of the heated area for the R-Z model of a disk-shaped, 1.5-m-thick heat, 0.364-acre heat source with a uniform heat load of 1264.6 kW/acre (representing 21 22.0-kW heaters), 711.36 kW/acre (21 12.38-kW heaters), 474.24 kW/acre (21 8.25-kW heaters), 364.53 kW/acre (21 6.3-kW heaters), 316.16 kW/acre (21 5.5-kW heaters), and 158.08 kW/acre (21 2.25kW heaters).
Figure 15. Vertical dry-out zone thickness as a function of time (a) and as a function of temperature (b) at the center of the heated area for the R-Z model of a disk-shaped, 1.5-m-thick heat, 0.364-acre heat source with a uniform heat load of 1264.6 kW/acre (representing 21 22.0-kW heaters), 711.36 kW/acre (21 12.38-kW heaters), 474.24 kW/acre (21 8.25-kW heaters), 364.53 kW/acre (21 6.3-kW heaters), 316.16 kW/acre (21 5.5-kW heaters), and 158.08 kW/acre (21 2.25-kW heaters).
Figure 16. Rate of temperature rise (a) and rate of increase of vertical dry-out zone thickness (b), \( \frac{d(h_d)}{dt} \), vs time at the center of the heated area for the R-Z model of a disk-shaped, 1.5-m-thick heat, 0.364-acre heat source with a uniform heat load of 1264.6 kW/acre (representing 21 22.0-kW heaters), 711.36 kW/acre (21 12.38-kW heaters), 474.24 kW/acre (21 8.25-kW heaters), 364.53 kW/acre (21 6.3-kW heaters), 316.16 kW/acre (21 5.5-kW heaters), and 158.08 kW/acre (21 2.25-kW heaters).
Figure 17. Rate of temperature rise (a) and rate of increase of vertical dry-out zone thickness (b), d(h_dz)/dt, vs time at the center of the heated area for 30-yr-old SNF for APDs of 114 and 57 kW/acre and a net recharge flux of 0 mm/yr. These calculations were done with the UZ model with a fixed-depth, constant-temperature water table and RIB Version 4 thermal conductivity data.