DEFINITION OF A FACILITY FOR EXPERIMENTAL STUDIES
OF TWO-PHASE FLOWS AND HEAT TRANSFER IN POROUS MATERIALS

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ABSTRACT

A facility-development effort is currently underway at Sandia National Laboratories in order to create an experimental capability for the study of two-phase, steam/water flows through a variety of porous media. The present paper describes the facility definition phase of this project. Equations are derived for the steady, adiabatic, macroscopically-linear two-phase flow of a single-component fluid through a porous medium, including energy transfer both by convection and conduction. These equations are then solved to give relative permeabilities for the steam and water phases as functions of known and/or measurable quantities. A viable experimental approach was thereby formulated, leading to the definition of facility components and instrumentation requirements, including the application of gamma-beam densitometry for the measurement of liquid-saturation distributions in porous media. Finally, a state-of-the-art computer code was utilized to numerically simulate the proposed experiments, providing an estimate of the facility operating envelope.

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INTRODUCTION

Energy-related research has expanded markedly in recent years in support of such key programmatic areas as nuclear waste isolation, nuclear reactor safety, geothermal energy extraction and enhanced oil recovery. An area of fluid mechanics common to all these engineering programs is that of two-phase flows in porous media. In particular, a detailed understanding of two-phase steam/water flows through porous media, both geologic and "manmade," has yet to be achieved.

Central to all such steam/water flows in porous media is the concept of relative permeabilities. This concept represents an attempt to extend Darcy's law for single-phase flows through porous media to the two-phase flow regime. In this regime, the flowrate of each phase is related to the macroscopic pressure gradient imposed on the flow through relative permeability parameters $k_L$ (for the liquid phase) and $k_G$ (for the "gas" or vapor phase), each expressed as a fraction of the bulk permeability ($k$) of the medium to all-liquid flow. Accurate "models" for $k_L$ and $k_G$ as functions of some independent flow variable (historically liquid saturation) are required if one is to solve the complex two-phase flow problems noted above. For example, a sensitivity study recently conducted by the present authors\textsuperscript{1} demonstrated the influence of "uncertainties" in such models on predicted geothermal reservoir performance. Further, it was shown that currently utilized models (e.g., Corey\textsuperscript{2}, based on non-steam/water data bases) possess serious deficiencies in relation to such problems.

Figure 1 shows a schematic of a generalized relative permeability model which incorporates all of the features ("limits")
potentially encountered for a two-phase liquid/vapor flow through a porous medium. Here $s$ is liquid saturation, defined as the fraction of the pore volume occupied by liquid; $f$, the dynamic quality of the flow, defined as the fraction of the total mass flowrate attributed to the vapor phase, $(W_G/W_L+W_G)$; $k'$, the bulk permeability of the medium to all-vapor flow at a specified pressure ($p$). The four saturation limits of interest, and their corresponding flow regimes, are defined as follows:

(a) $s(f+0)$ is the saturation at which $f$ goes to zero. Thus, for $s>s(f+0)$, the vapor phase does not flow, i.e., it is "trapped" ($W_G=0$).

(b) $s(k_L+k)$ is the saturation at which $k_L$ goes to $k$. Thus, for $s>s(k_L+k)$, the trapped vapor phase no longer influences the flow of the liquid phase.

(c) $s(f+1)$ is the saturation at which $f$ goes to unity. Thus, for $s<s(f+1)$, the liquid phase does not flow, i.e., it is "trapped" ($W_L=0$).

(d) $s(k_G+k')$ is the saturation at which $k_G$ goes to $k'$. Thus, for $s<s(k_G+k')$, the trapped liquid phase no longer influences the flow of the vapor phase.

For certain geologic materials, the saturation regime in which a trapped phase can exist may be quite extensive (e.g., Fig. 2-5 of Collins\textsuperscript{3} presents data for sandstone cores indicating potential values for $s(f+1)$ increasing from 0.2 to 0.7 as $k$ decreases from $10^{-12}$ m\textsuperscript{2} to $10^{-15}$ m\textsuperscript{2}). Should a trapped phase initially reside solely within the non-interconnected regions of the pore space, it would not be expected to influence the flow of the other phase. However, as the volume fraction of a trapped phase increases, it may also begin to reside in portions of the interconnected flow channels within the porous medium. At this point, the trapped phase would begin to adversely affect the flow
of the other phase. For these reasons, restrictions must be imposed on the above noted limits:

$$s(k_G+k') < s(f+1)$$ and $$s(k_L+k) > s(f+0)$$

Further, since the vapor phase is a "gas," $$k' > k$$ due to molecular effects, with $$k'$$ approaching $$k$$ "from above" only as pressure becomes infinite. It can be seen, therefore, that the functions $$k_L(s)$$ and $$k_G(s)$$ depend on the "physical properties" of both the working fluid and the porous medium. Hence, no universal correlation for these functions can exist. Such models must be determined experimentally for each fluid/porous medium combination of interest.

A facility-development effort is currently underway to allow fundamental experiments of this type to be carried out. The objective of the present paper is to report on the facility-definition phase of this research.

**THEORETICAL CONSIDERATIONS**

One-dimensional, forced-convection flows are of primary interest in the present study, i.e., two-phase, steam/water flows will be "forced" through cylindrical columns (or "cores") of porous material under the influence of a superimposed pressure gradient. Isothermal flow, and adiabatic flow, represent the two limiting thermodynamic processes for this stated problem.

A review of phase diagrams for pure water (see Figure 2) shows that the isothermal case would require heat to be transferred from the "surroundings" to the fluid at the precise rate to maintain an evaporation front, over zero spatial distance, in order to achieve a constant temperature/decreasing pressure flow. Under these flow conditions, no two-phase flow region could exist since temperature and pressure are not
independent variables for mixtures of steam and water in thermo-
dynamic equilibrium, i.e., constant temperature would require
constant pressure and, therefore, would eliminate the driving
potential (pressure gradient) to maintain liquid/vapor motion.

The adiabatic boundary condition would require that no
heat be transferred from the "surroundings" to the fluid,
i.e., the fluid, the porous media, and the "container" walls
must be in thermal equilibrium. In this case, two-phase flow
regions of finite extent could exist, with vaporization occurring
as a result of decreasing pressure (and temperature) in the
direction of flow.

Between these two limiting cases exist an infinite number
of thermodynamic/flow processes wherein heat may be added to,
or taken from, the liquid/vapor mixture as it traverses the
test medium. In all of these cases, the added experimental
complexity of precisely measuring the radial heat transfer
distribution along the core would then arise.

Based on these considerations, Miller\textsuperscript{4} theoretically and
experimentally investigated the "steady, adiabatic, macro-
scopically linear, two-phase flow of a single-component fluid
(propane) through a horizontal column of porous medium". His
stated objectives were "to find the total weight rate of flow
and the pressure distribution along the column for a given
inlet pressure and temperature, a given exit pressure or tempera-
ture, and given characteristics of the test fluid and porous
medium". In accomplishing these objectives, he did not generate
a new relative-permeability data set, rather he used the relative-
permeability data of Wyckoff and Botset\textsuperscript{5}, "shifted" to match
his own experimental conditions. Further, his treatment of this problem was limited to convection-dominated flows.

In the present effort, the analysis of Miller\textsuperscript{4} is extended to include energy transfer in the flow direction via both convection and conduction, thereby allowing application of the resulting equations to the lower-permeability regime typical of consolidated porous media. The overall flow system is still assumed to be one-dimensional, steady-state, and adiabatic, with the single exception of a known/measurable heat addition to the flow prior to its entry into the porous medium. This controlled heat input allows for systematic variations in the mixture quality at the core entry, ensuring that the complete saturation regime of interest ($0 < s < 1$) can be achieved. Figure 3 gives a schematic of the problem under study; the analysis proceeds as follows.

The continuity equation yields

$$W_O = W_L + W_G = \text{constant} \quad (1)$$

The energy equation for this problem can be written as

$$W_O H_O + Q_O = W_L H_L + W_G H_G + Q = \text{constant} \quad (2)$$

where $Q$ is the total conductive energy flux in the flow direction through the liquid ($L$), the vapor ($G$), and the matrix material ($M$):

$$Q = Q_L + Q_G + Q_M = -\bar{K} A \frac{dT}{dx} \quad (3)$$

In eq. (3), the total conductivity, $\bar{K}$, can be written as

$$\bar{K} = \$ K_{L,G} + (1 - \$) K_M \quad (4)$$

where,

$$K_{L,G} = s K_L + (1 - s) K_G \quad (5)$$

The momentum equations are given by Darcy's law written for each phase
Capillary effects may potentially be encountered for two-phase flows in low-permeability, consolidated porous media. The defining equation for capillary pressure, $p_c$, taken to be a function solely of the "wetting" (liquid) phase saturation, is given by

$$p_G - p_L = p_c(s)$$

(8)

Differentiating with respect to $x$ yields

$$\frac{dp_G}{dx} - \frac{dp_L}{dx} = \frac{dp_c}{ds} \cdot \frac{ds}{dx}$$

(9)

In the present case, an overall macroscopic pressure gradient, $(p_2-p_1)/L$, will be maintained across the core in order to drive the flow. "Point" measurements of pressure along the core, between stations 1 and 2, will yield $p(x)$ and thus $(dp/dx)$. Independent measurements of $T(x)$ will also be made. Pressure, temperature "pairs", for any $x$, can then be compared to the equilibrium saturation curve for pure water; should significant departures be found, capillary effects may be indicated.

The problem would then arise as to the interpretation of the measured "local" pressure and pressure gradient. One might argue that the measured pressure, and pressure gradient, would be most representative of the vapor phase, and hence $(dp_L/dx)$ could be calculated from eq. (9) using measured $(ds/dx)$.
In this manner, measurements (to be outlined below) can be converted into empirical correlations for $k_L(s)$ and $k_G(s)$.

However, it is not certain that the measured pressure would be equal to that of the vapor phase. Further, the procedure outlined in the paragraph above would require the application of results measured under "static" conditions (i.e., the $p_C(s)$ curve) to a "dynamic" flow situation. For example, it has not been proven that the irreducible water saturation measured in a "static" $p_C(s)$ experiment would be identical to the limit $s(f+1)$ obtained in a "dynamic" relative-permeability experiment (recall Figure 1).

Obviously, the above-described procedure would introduce uncertainties comparable to those inherent in using the (single) measured pressure gradient in both eqs. (6) and (7) in order to reduce the data.

In the derivation to follow, we assume negligible capillary effects, such that

$$
\left( \frac{dp}{dx} \right)_{\text{measured}} = \frac{dp_G}{dx} = \frac{dp_L}{dx}
$$

In this manner, measurements (to be outlined below) can be converted into empirical correlations for $k_L(s)$ and $k_G(s)$. Capillary effects, should they be significant, will then be implicitly included in these empirically-derived correlations through the limits $s(f+0)$ and $s(f+1)$, i.e., through the extent of each trapped-phase saturation regime.

The following definitions are now introduced:

$$
\phi = \frac{V_{\text{pores}}}{V_{\text{core}}}
$$
Combining continuity, eq. (1), and the definition of dynamic quality, eq. (13), yields

$$f = \frac{W_G}{W_L + W_G}$$  \hspace{1cm} (13)

Substituting eqs. (13) and (14) into the energy equation, eq. (2), and solving for $f$ yields,

$$f = \frac{W_o (H_o - H_L) + (Q_o - Q)}{W_o H_{LG}}$$  \hspace{1cm} (15)

where,

$$H_{LG} \equiv (H_G - H_L)$$  \hspace{1cm} (16)

Substituting the value of $W_L$ from eq. (14) into eq. (6), the value of $W_G$ from eq. (13) into eq. (7), and solving for $k_L$ and $k_G$ yields,

$$k_L = \frac{W_o}{k_A} \cdot \frac{dp}{dx}$$  \hspace{1cm} (17)

$$k_G = \frac{f W_o}{\kappa A \cdot \frac{dp}{dx}}$$  \hspace{1cm} (18)

Equations (15), (17) and (18) thus provide solutions for $k_L$ and $k_G$ in terms of known and/or measurable quantities. These are summarized below:
(a) required measurements: $W', H'_o, Q', k, A, p(x), T(x), \phi(x), s(x)\) .

(b) required properties: $\mu_L(p,T), \mu_G(p,T), \nu_L(p,T), \nu_G(p,T), K_L, K_G, K_M$ .

As was noted in discussions concerning Figure (1), $k_G$ may also be a function of absolute pressure due to molecular effects. When the pore dimensions become comparable to the molecular mean free path of the flowing vapor, a slip-flow/non-viscous regime is attained wherein Darcy's law is no longer strictly valid. A family of $k_G$ vs. $s$ curves, each at constant pressure, would therefore be required in order to completely describe any steam/water relative-permeability model.

Extending the work of Klinkenberg, which related single-phase gas and liquid permeabilities (see Chapter 7 of Scheidegger$^7$), we now postulate that

$$k_G = k_G(p+\infty) + \frac{b}{p}$$

(19)

where both $k_G(p+\infty)$ and $b$ are functions of saturation. In this manner, a composite plot of $k_G$ vs $(1/p)$, with $s$ as a parameter, can be generated from the combined results of many separate experimental runs (see schematic of Figure 4). Values for $k_G(p+\infty)$, for any specified value of $s$, can then be obtained by extrapolation and results used to plot the single "limiting" $k_G$ vs $s$ curve illustrated in Figure 1.

Finally, we wish to show that equations (17) and (18), for $k_L$ and $k_G$, are consistent with the limits of the generalized relative-permeability model put forth in Figures 1 and 4. To assist in this endeavor, the following expressions are found useful.
From mass flux considerations, at any transverse station along the core, we can write

\[ W_L = \frac{A \phi s u_L}{v_L} \]  

(20)

\[ W_G = \frac{A \phi (1-s) u_G}{v_G} \]  

(21)

where \((A \phi s)\) is the effective cross-sectional area occupied by liquid and \((A \phi (1-s))\) is the effective cross-sectional area occupied by vapor. Average, in-situ, velocities of the individual phases at any transverse station are represented by \(u_L\) and \(u_G\). It should be noted that these velocities need not be equal, i.e., "slip" between the phases can occur. Substituting eqs. (20) and (21) into the defining equation (13) for \(f\), and rearranging, yields

\[ f = \frac{1}{1 + \left( \frac{s}{1-s} \right) \left( \frac{v_G}{v_L} \right) \left( \frac{u_L}{u_G} \right)} \]  

(22)

Therefore,

\[ f = \left[ \frac{(1-s) u_G}{(1-s) u_G + s \left( \frac{v_G}{v_L} \right) u_L} \right] = \frac{W_G}{W_L + W_G} \]  

(23)

We now equate the right-hand sides of eqs. (6) and (20) and eqs. (7) and (21). Forming the ratio of the resultant expressions while invoking eq. (10) yields

\[ \left( \frac{s}{1-s} \right) \left( \frac{u_L}{u_G} \right) = \left( \frac{k_L}{k_G} \right) \left( \frac{\mu_G}{\mu_L} \right) \]  

(24)
Solving eq. (24) for the phase velocity ratio yields

\[
\left( \frac{u_L}{u_G} \right) = \left( \frac{1-s}{s} \right) \left( \frac{\mu_G}{\mu_L} \right) \left( \frac{k_L}{k_G} \right)
\]

(25)

For single-phase flows of liquid, and of vapor, Darcy's law is written

\[
W_{L,s=1} = -\frac{kA}{\mu_L v_L} \cdot \frac{dp}{dx}
\]

(26)

\[
W_{G,s=0} = -\frac{k' A}{\mu_G v_G} \cdot \frac{dp}{dx}
\]

(27)

In the limit of no vapor flow \((W_G \rightarrow 0)\), while allowing for the possibility of trapped vapor presence \((s \neq 1)\) we have:

- from eq. (21), \(u_G \rightarrow 0\)
- from eq. (1), \(W_o + W_{L,s=1}\)
- from eq. (23), \(f \rightarrow 0\)
- from eqs. (17), (26), \(k_L + \frac{(1) W_{L,s=1}}{W_{L,s=1}} < 1\)
- from eq. (18) \(k_G \rightarrow 0\)

Thus, under the stated conditions of no vapor flow, but with trapped vapor presence, the relative permeability for the gas phase must be zero, while the relative permeability for the liquid phase can be less than, or equal to, unity. In the limit where the trapped vapor no longer influences the liquid-phase flow,

\[
W_{L,s=1} + W_{L,s=1}
\]

and

\[k_L \rightarrow 1.0\]

even though \(s\) may still be less than unity.
In the limit of no liquid flow \((W_L + 0)\), while allowing for the possibility of trapped liquid presence \((s\neq 0)\) we have:

- from eq. (20), \(u_L + 0\)
- from eq. (1), \(W_0 + W_G,s\neq 0\)
- from eq. (23), \(f + 1.0\)

- from eqs. (18),(27), \(k_G - k' \cdot \frac{W_G,s\neq 0}{W_G,s=0} > 1\)

- from eq. (17), \(k_L + 0\).

Thus, under the stated conditions of no liquid flow, but with trapped liquid presence, the relative permeability for the liquid phase must be zero, while the relative permeability for the gas phase can be greater than, equal to, or less than unity. In the ultimate limit where the trapped liquid no longer influences the vapor-phase flow,

\[ W_G,s\neq 0 + W_G,s=0 \]

and

\[ k_G + \frac{k'}{k} > 1 \text{ for } p < \infty \]

even though \(s\) may still be greater than zero.

**PROPOSED EXPERIMENTAL APPROACH**

Results derived in the previous section serve to define required boundary conditions, inputs, and measurements necessary to experimentally establish steam/water relative-permeability correlations. In the present section, the proposed experimental approach will be outlined, complete with a discussion of principle system components and a review of the instrumentation system to
be utilized for measurements of material-porosity, and liquid-
saturation, distributions.

Figure 5 shows a schematic of the proposed facility. A con-
stant flowrate of liquid water, at a constant temperature level,
will be delivered to the test apparatus by a dual-cylinder,
positive-displacement pump. The unit under procurement possesses
a capability for delivering known/constant flowrates from 1 to
\(10,000\) cc/hour at temperatures up to \(180^\circ\)C. Further, these
flowrates can be maintained at present levels against any back
pressure currently projected to occur at the entry to the core
\((p_{1,\text{max}} \leq 50\) bars). The product \(W_0H_0\) is thus known and constant.

Between stations labeled "zero" and "one" in Figure 5, some,
or all, of the liquid water will be converted to steam via the
addition of a measurable quantity of heat. This energy input
will be accomplished electrically from resistance heat generated
by cartridge heaters immersed directly within the flow tubes.
Additional resistance heat may also be input to the divider plate
at station one, then transferred either to the working fluid
immediately prior to its entry into the core, or directly to
the core itself, via conduction. In this manner, all of the
electrical energy input will be transferred directly into the
flow system, hence \(Q_0\) is known.

The entire apparatus between stations zero and two will
be designed to minimize thermal losses to a negligible percentage
of the total energy input \((W_0H_0+Q_0)\). Thermal insulation,
coupled with a double-wall/"vacuum-bottle" design, will be
incorporated to meet this need, thereby ensuring the validity
of the adiabatic-flow assumption.
To avoid separation of the two phases within the "boiler", flow lines will pass through the divider plate, the mixture of steam and water being introduced directly into the porous medium under study. The entire apparatus from station zero to station two will be mounted vertically to eliminate flow stratification in the transverse direction. The downstream end of the test section will be maintained at a constant pressure level via a pressure regulator.

Unconsolidated porous media (e.g., sands) will be packed into the cylindrical, metallic test section and physically separated from adjacent facility parts by wire-mesh screens. This technique allows for flow passage without "particle migration". Consolidated porous media (e.g., rock cores) must be mounted in a manner which eliminates flow bypass channels between the core outer surface and the containment-wall inner surface. This can be accomplished in one of two ways: (1) fabricating the test section with a flexible inner wall, then subjecting the volume between the test-section's outer/rigid wall and its inner/flexible wall to an "overburden pressure", thereby "squeezing" the inner wall against the core surface, or (2) physically bonding the core outer surface to the containment-wall inner surface with a high-temperature epoxy.

Conventional instrumentation is available to meet most of the measurement requirements listed earlier. The two most-challenging variables to be measured are material porosity and liquid saturation. Spatial distributions for these two variables will be experimentally determined using a traversable gamma-beam densitometer. Reference 8 describes the application of this
non-intrusive diagnostic technique to the problem under investigation. Principal features of this technique are reviewed below (see Figure 6).

The interaction of gamma rays with matter is described by the exponential equation

\[
\frac{I}{I_0} = \exp \left[ \sum_{i=1}^{n} (-\mu_i z_i) \right]
\]

\(I_0\) is the initial beam intensity, \(I\) is the attenuated beam intensity, and \(\mu_i z_i\) is the product of the total linear attenuation coefficient (\(\mu_i\)) and the total integrated path length normal to the beam (\(z_i\)) for material \(i\).

The attenuated beam intensity \(I\) which strikes the detector crystal is converted by the sensing electronics to a count rate, \(R\), in a non-linear fashion given by

\[
R = \frac{I}{1 + \tau I}
\]

(29)

Here, \(\tau\) is a characteristic time constant of the sensing electronics and is a measure of the "dead time" during which the system cannot "count" all arriving pulses from the detector crystal. For the system used herein, \(\tau \approx 3 \times 10^{-6}\) sec. Equations (28) and (29) are the defining equations for the gamma-beam system.

The primary independent variable to be measured with the gamma-beam system is liquid saturation, \(s\), hence what is required is a "calibration curve" of \(s\) as a function of \(R\) for each specific experimental arrangement. Any such calibration should be obtained "in-situ", i.e., with the porous medium of interest mounted within its confining walls, or "test section", and the gamma beam aligned perpendicular to the facility centerline.
Expanding eq. (28) we have

\[
\frac{I}{I_0} = \exp \left[ -(\mu z)_{\text{walls}} - (\mu z)_{\text{core}} - (\mu z)_{H_2O} \right]
\]  

(30)

From the definition of liquid saturation we have

\[
s = \frac{\text{Volume of Liquid}}{\text{Volume of Void}} = \frac{z_{H_2O}}{z_{\text{void}}}
\]

(31)

From the definition of porosity we have

\[
\phi = \frac{\text{Volume of Void}}{\text{Volume of Core}} = \frac{z_{\text{void}}}{z_{\text{core}}}
\]

(32)

Combining eqs. (31) and (32) yields

\[
z_{H_2O} = sz_{\text{void}} = s\phi z_{\text{core}}
\]

(33)

Substituting eq. (33) into eq. (30) gives

\[
\frac{I}{I_0} = \exp \left[ -(\mu z)_{\text{walls}} - (\mu z)_{\text{core}} - H_2O s\phi z_{\text{core}} \right]
\]

(34)

Equations (29) and (34) thus serve to relate measured count rates to liquid saturation for any intensity regime utilized. Successive measurements of beam attenuation define all quantities of interest:

(a) test section empty yields \( \mu_{\text{wall}} \).

(b) test section plus unsaturated \((s=0)\) porous medium yields \( \mu_{\text{core}} \).

(c) test section plus completely saturated \((s=1)\) porous medium yields \( \phi \).

(d) test section plus partially saturated \((0<s<1)\) porous medium (the actual two-phase flow situation) yields \( s \).

Should all data be acquired at low-intensity levels \((I<2\times10^4 \text{ gammas/sec})\), a regime wherein no "dead-time" occurs, then eqs. (29) and (34) can be combined to give a direct relationship between measured count rate and liquid saturation:
where \( R_L \) is the measured count rate for \( s = 1 \) and \( R_G \) is the measured count rate for \( s = 0 \).

An error analysis was also conducted in Reference 8 to define the relationship between the absolute error in any saturation measurement, \( |ds| \), and various system and experimental parameters. Results are summarized by eqs. (36) and (37).

\[
|ds| = \left[ \sqrt{Rt} \ (1 - \overline{\tau}_R) \ln C \right]^{-1}
\]

(36)

\[
t = R^{-1} \left[ |ds| \ (1 - \overline{\tau}_R) \ln C \right]^{-2}
\]

(37)

where \( C \) is the system "contrast", \((I_G/I_L)\).

Equation (36) shows that, for a given system contrast, \( |ds| \) is primarily dependent on the inverse square root of the total number of counts, the term \((1 - \overline{\tau}_R)\) being of order unity for applications considered herein. Equation (37) shows that for a given contrast, and thus a specified \( R \) regime \((R_L < R < R_G)\), the minimum count time is inversely proportional to the square of the maximum acceptable error.

For a given core diameter, material porosity directly affects the maximum path length of water which the beam must traverse (recall eq. (33)). The influence of porosity is thus implicitly included in eqs. (36) and (37) through \( R \) and \( C \). A series of calculations was undertaken to explicitly define the effects of porosity on required count time. Results are shown in Figure 7. For a given beam intensity, \( I_o \), \( t \) was found to depend on the product \( \phi \cdot |ds| \) raised to the minus two power. Results also showed that, for \( \phi \cdot |ds| \) fixed, \( t \) was essentially independent of core diameter (over a factor of two-to-three regime) once
attenuation due to the core material and the water in its void space equaled, or exceeded, that caused by the test-section walls (2.54 cm of steel for these calculations). Results of the error analysis thus provided an important input to facility definition.

It should be noted here that the facility and instrumentation described above can also be applied to problems involving porosity variations with time (e.g., geochemical alterations of geologic materials due to water flows through them, or filtration of particles from liquid flows by passage through porous media) as well as to problems associated with moisture migration (drying) in porous media.

DEFINITION OF THE FACILITY OPERATING ENVELOPE

The proposed experiments were numerically simulated using a state-of-the-art computer code called SHAFT. This code treats both one- and two-phase flows of pure water through porous media in both the natural- and forced-convection regimes. Energy transfer via both convection and conduction is accounted for. At present, this simulator does not explicitly allow for capillary effects (i.e., \( p_L = p_G \)); such effects can be implicitly introduced to the calculations through use of a relative-permeability model which includes trapped-phase saturation regimes. However, since determination of such models is an objective of this ongoing research, the "correct" model to use is not known a priori. Consequently, for the present calculations, a reference-case relative-permeability model was used which allowed for no trapped phases. Results so generated serve as "design guidelines" only.

Prior to undertaking such numerical simulations, it was first necessary to estimate liquid flowrates \( (W_o) \) and energy-input requirements \( (Q_o) \) as a function of bulk permeability. These were
calculated using eq. (26) plus a representative average value for $H_{LG}$ of 2000 KJ/Kg. Results are shown in Figures 8 and 9.

Superimposing the previously-stated pump flowrate capabilities (1 to $10^4$ cc/hr) onto Figure 8 shows that experiments can be conducted over a wide bulk-permeability range, from $10^{-16} \text{m}^2$ (representative of tuff, a consolidated porous material of compressed volcanic ash) to $10^{-13} \text{m}^2$ (representative of consolidated sandstone). Flows through unconsolidated sands ($k = 10^{-12} \text{m}^2$) would require either a higher-flowrate capability than presently available in such positive-displacement pumps, or cores significantly longer than those presently under consideration (e.g., $L > 3.0$ meters).

Figure 9 shows energy inputs ($Q_o$) required to completely vaporize the entire flowrate of saturated liquid water ($W_o$) corresponding to each $\Delta p, k$ pairing in Figure 8. Energy input levels were found to range from the order of several watts for $k = 10^{-16} \text{m}^2$ to the order of 10 kilowatts for $k = 10^{-13} \text{m}^2$.

Using these results as inputs, the SHAFT code was then utilized to predict steady-state saturation, pressure, and temperature distributions for flows through 0.3 meter-long cores of $k = 10^{-16}, 10^{-14}$ and $10^{-12} \text{m}^2$. Representative results are shown in Figures 10 through 17.

Figure 10 shows calculated peak pressures at core entry for all cases considered (see parameter values listed there). Peak pressure was found to scale with $Q_o^n$, where $n$ increased from 0.41 at $k = 10^{-12} \text{m}^2$ to 0.87 at $k = 10^{-16} \text{m}^2$. Maximum pressure levels were predicted to remain below 50 bars (=750. psia) for those $Q_o$ values required to cover the entire saturation regime ($0 < s < 1$) for each permeability under study.
Figure 11 illustrates the comparative influence of convection and conduction on the total energy-transfer rate for the three permeabilities considered. Convection was found to dominate for \( k \leq 10^{-14} \text{m}^2 \), while conduction and convection were predicted to be of the same order of magnitude for \( k = 10^{-16} \text{m}^2 \).

Figures 12 through 14 show predicted \( s(x) \), \( p(x) \) and \( T(x) \) distributions, with \( Q_0 \) as a parameter, for a convection-dominated flow \( (k = 10^{-14} \text{m}^2) \), while Figures 15 through 17 show predicted distributions for a combined convection/conduction case \( (k = 10^{-16} \text{m}^2) \). Fundamental differences between these two regimes were predicted to occur and are best illustrated by the respective \( s(x) \) distributions.

For the convection-dominated case, as \( Q_0 \) was increased, a vaporization front was predicted to move upstream, towards the core entry. Once there, the initial saturation level was predicted to steadily decrease with increasing \( Q_0 \) due to the increased quality of the entry flow. More importantly, for all \( Q_0 \) levels, once a two-phase flow region was predicted to occur, \( s \) was predicted to continually decrease in the direction of flow. In such cases, continuous vaporization (or "boiling") was predicted to occur due to the convection-dominated pressure drop in the direction of flow.

For the convection/conduction case, \( s \) at the core entry was also predicted to decrease with increasing \( Q_0 \) due to the increased quality of the entry flow. Here, however, \( s \) was predicted to remain essentially constant with \( x \) for a finite distance into the core, followed by an abrupt, "shock-like", condensation
front resulting in a return to the all-liquid state. (Similar results were observed in the transient, condensing-flow, experiments of Nilson and Montoya\textsuperscript{10}.) In such cases, energy transfer along the core via conduction acts to counter the potential for any increased "boiling" of the liquid phase due to decreasing pressure in the direction of flow.

Predicted results showed that the complete saturation regime could be created in consolidated porous media of interest while maintaining "acceptable" energy and mass flowrate inputs and "containable" pressure and temperature levels.

**SUMMARY**

Combined theoretical and numerical analyses, coupled with the proposed application of state-of-the-art instrumentation, have resulted in the definition of a viable experimental approach to the problem of steam/water flows in porous media. A facility-development effort based on these results is currently underway.
REFERENCES


NOMENCLATURE

A  core cross-sectional area
b  constant in eq. (19)
C  gamma-beam system contrast, \((I_G/I_L)\)
d  collimator-tube diameter
f  dynamic quality, \((W_G/W_L + W_G)\)
H  enthalpy
I  attenuated beam intensity
I_0  unattenuated beam intensity
k  bulk permeability to all-liquid flow
k'  bulk permeability to all-vapor flow
k_G  relative permeability for gas or vapor
k_L  relative permeability for liquid
K  thermal conductivity
K  average thermal conductivity of core material and flowing phases
L  core length
p  pressure
P_C  capillary pressure
\(\Delta P\)  \(|P_2 - P_1|\)
Q_0  heat input to flow system
Q  conductive heat flux in x direction
R  count rate
s  liquid saturation
|ds|  absolute error in any s measurement
t  count time
T  temperature
u  phase velocity
v  specific volume
NOMENCLATURE (Con't)

V volume
W mass flowrate
x coordinate in flow direction
z pathlength of any substance which gamma beam must traverse
\mu viscosity; also, linear-attenuation coefficient when used in reference to gamma-beam system
\bar{T} electronic-system time constant
\phi material porosity

Subscripts
core porous material
G gas or vapor phase
H_2O liquid water
L liquid phase
M matrix material of core
0,1,x,2 stations along apparatus, see Fig. 3
pores(void) refers to pore or "void" space within porous material
s = 0 no liquid presence
s \neq 0 liquid presence allowed
s = 1 no vapor presence
s \neq 1 vapor presence allowed
walls containment or "test-section" walls
FIGURES

1. Schematic of a Generalized Relative-Permeability Model
2. State Diagram Schematics for Isothermal and Adiabatic Flow of a Pure Substance
3. Flow Schematic for Problem under Study
4. Pressure-Correction Procedure for Vapor Relative Permeability
5. Schematic of Proposed Facility
6. Schematic of Gamma-Beam Densitometer
7. Minimum Count Time vs. Porosity-Saturation Error Product
8. Facility Flowrate Requirements
9. Facility Heat-Input Requirements
10. Calculated Peak Pressures as a Function of Heat Input
11. Relative Influence of Convection vs. Conduction on Energy Transfer as a Function of Bulk Permeability
12. Calculated Saturation Distributions, $k = 10^{-14} \text{m}^2$
13. Calculated Pressure Distributions, $k = 10^{-14} \text{m}^2$
14. Calculated Temperature Distributions, $k = 10^{-14} \text{m}^2$
15. Calculated Saturation Distributions, $k = 10^{-16} \text{m}^2$
16. Calculated Pressure Distributions, $k = 10^{-16} \text{m}^2$
17. Calculated Temperature Distributions, $k = 10^{-16} \text{m}^2$
ab: ISOTHERMAL FLOW
ac: ADIABATIC FLOW
FLOW SCHEMATIC

WITH EXCEPTION OF KNOWN $Q_0$, SYSTEM IS INSULATED/ADIABATIC

FLOW IS ONE-DIMENSIONAL AND AT STEADY STATE
$k_G$ VALUE DEFINED BY EXTRAPOLATION TO INFINITE $p$, FOR CONSTANT $s$

$0 \leq s \leq s_G (k_G - k')$

DATA POINTS

TYPICAL "STATE PATH" FOR TWO-PHASE FLOW THROUGH THE CORE

$0 \leq s(1-O) \leq s \leq 1$
FACILITY SCHEMATIC

LIQUID WATER RESERVOIR

DUAL-CYLINDER, POSITIVE-DISPLACEMENT PUMP WITH CONSTANT-TEMPERATURE CONTROL.

CORE HOLDER

POROUS MEDIUM

INSULATED SYSTEM

COMBINATION TUBE-ENTRY PLATE AND CONDUCTION HEATER

PRESSURE REGULATOR ($p_2 =$ CONSTANT)

POWER SUPPLY

100% OF ELECTRICAL POWER GOES INTO LIQUID, OR INTO LIQUID AND CONDUCTION HEATER

EXHAUST

CONDENSOR

BOILER

EXIT CHAMBER

$Q_0$
\[ I_0 = I_0 \left( d^2 \right) \]

Spatial Resolution \( = f(d) \)

\[ I = I_0 e^{i=1} \sum_{i=1}^{n} (-\mu_i z_i) \]

\[ R = \frac{I}{1 + \tau I} \]

\( \tau = \text{constant} \)
$7.62 \leq z_{\text{CORE}} \leq 15.24 \text{ cm}$

$s = 1$

$\phi \cdot |ds|$

$t (\text{sec})$

$10^4$

$10^3$

$10^2$

$10$

$1$

$0.1$

$10^{-4}$

$10^{-3}$

$10^{-2}$

$10^{-1}$

$10^2$

$10^3$

$10^4$

$10^5$

$10^6$

$10^7$

$10^8$

$10^9$

$10^{10}$

$10^{11}$
\[ W_0 \text{ (cc/hr)} \]

\[ \Delta p \text{ (BARS)} \]

SATURATED LIQUID WATER

\[ A = 8.107 \times 10^{-3} \text{ m}^2 \]
\[ \mu_L = 1.54 \times 10^{-4} \text{ KG/m-sec} \]
\[ v_L = 0.0011275 \text{ m}^3/\text{KG} \]
\[ L = 0.3 \text{ m} \]
Saturated Liquid Water

\[ A = 8.107 \times 10^{-3} \text{ m}^2 \]
\[ \mu_L = 1.54 \times 10^{-4} \text{ KG/m-sec} \]
\[ v_L = 0.0011275 \text{ m}^3/\text{KG} \]
\[ L = 0.3 \text{ m} \]
\[ H_{LG} = 2000 \text{ KJ/KG} \]
### Numerical Simulations of Proposed Experiments

<table>
<thead>
<tr>
<th>SYMBOL</th>
<th>$k$ (m$^2$)</th>
<th>$W_0$ (KG/sec), (cc/hr)</th>
<th>$H_0$ (KJ/KG)</th>
</tr>
</thead>
<tbody>
<tr>
<td>-</td>
<td>$10^{-16}$</td>
<td>$10^{-5}$ 41</td>
<td>711</td>
</tr>
<tr>
<td>-</td>
<td>$10^{-14}$</td>
<td>$10^{-3}$ 4100</td>
<td>711</td>
</tr>
<tr>
<td>-</td>
<td>$10^{-12}$</td>
<td>$5 \times 10^{-2}$ 2.05 $\times 10^5$</td>
<td>612</td>
</tr>
</tbody>
</table>

$W_0$ converted to all steam for $H_{LG} = 2000$. KJ/KG

- $P_1$, MAX (BARS)
- $Q_0 = 0$
- $L = 0.3$m
- $\phi = 0.23$
- $p_2 = 1$ BAR

QO (KWATTS)
\( \dot{Q}_0 = 0; \quad s_1 = 1.0 \)

- **Convection Ratio**: Dashed line
- **Conduction Ratio**: Solid line

- \( k (m^2) \)
  - \( 10^{-12} \)
  - \( 10^{-14} \)
  - \( 10^{-16} \)

- \( x \) (m)
  - 0
  - 0.1
  - 0.2
  - 0.3

\[ \frac{\dot{Q}}{\dot{Q} + H (0)} \]
INITIAL LEVEL

$k = 10^{-14} \text{ m}^2$

- 0.1 KW
- 0 KW
- 0.5 KW
- 1.0 KW
- 1.5 KW

$s$

$x (m)$
The graph shows the temperature distribution $T$ in degrees Celsius as a function of position $x$ in meters for different heat fluxes $Q$ in kilowatts ($0$, $0.1$, $0.5$, $1.0$, $1.5$, $1.5$ kW) with a thermal conductivity $k = 10^{-14} \text{ m}^2$. The x-axis represents the position $x$ in meters, ranging from $0$ to $0.3$ m, and the y-axis represents the temperature $T$ in degrees Celsius, ranging from $100$ to $300^\circ \text{C}$. The curves indicate how temperature decreases with increasing distance for different power levels.
$k = 10^{-16} \text{ m}^2$

- $0.02 \text{ KW}$
- $0.01 \text{ KW}$
- $0 \text{ and } 0.005 \text{ KW}$
$k = 10^{-16} \text{ m}^2$

Temperature ($T$) vs. distance ($x$) graph with lines labeled 0.02 KW, 0.01 KW, and 0.005 KW.