Erosion of Noncohesive Sediment by Ground Water Seepage: Lysimeter Experiments and Stability Modeling

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Seepage may be a significant mechanism of streambank erosion and failure in numerous geographical locations. Previous research investigated erosion by lateral subsurface flow and developed a sediment transport model similar to an excess shear stress equation. As a continuation of this earlier research, slope destabilization driven by lateral, subsurface flow was studied to further verify the recently proposed sediment transport model. Laboratory experiments were performed using a two-dimensional soil lysimeter. The experiments were conducted on two sandy soils: a field soil (loamy sand) and sieved sand with greater sand content and less cohesion. A series of seven lysimeter experiments were performed for the two different sands by varying the bank slope (90, 60, 45, 36, and 26°). Flow and sediment concentrations were measured at the outflow flume. Pencil-size tensiometers were used to measure soil pore-water pressure. A slight modification of the existing seepage sediment transport model adequately simulated lysimeter experiments for both non cohesive soils without modifying the seepage parameters of the excess shear stress equation, especially for bank angles >45°. The research then determined whether integrated finite element and bank stability models were capable of capturing both small- and large-scale sapping failures. The models predicted large-scale failures for bank angles >45° in which tension cracks formed on the bank surface. The models failed to predict collapses for bank angles <45° in which tension cracks formed on the seepage face. The failure to predict collapse was hypothesized to be due to the assumption of circular arc slip surfaces. More analytically complex stability approaches are needed to capture bank slope undermining.

Abbreviations: Fs, factor of safety; LS, loamy sand; LTC, Little Topashaw Creek.

Studies have shown that sediment from streambanks can account for as much as 30 to 80% of watershed sediment yields (Bull and Kirkby, 1997; Simon and Darby, 1999; Sekely et al., 2002; Evans et al., 2006). Streambank erosion and subsequent failures due to undercutting are typically considered to be a result of fluvial toe erosion, removal of negative soil pore-water pressure, and removal of confining pressure during the recession of streamflow following storm events (Simon et al., 2000).

The role of subaerial processes, or the weakening and weathering of bank material by soil moisture, on streambank erosion have also been highlighted (Rinaldi and Casagli, 1999; Prosser et al., 2000; Owoputi and Stolte, 2001; Couper, 2003). The influence of the soil pore-water pressure distribution contributing to a reduction in soil shear strength has been emphasized by many researchers as a contributing mechanism to streambank failure (Simon and Curini, 1998; Froese et al., 1999; Casagli et al., 1999; Simon et al., 2000; Couper, 2003; Rinaldi et al., 2004; Schörghofer et al., 2004). Recent conceptual models of streambank stability include the role of saturated and unsaturated soil pore water pressures. When streambanks are unsaturated, stability is enhanced (Darby and Thorne, 1996; Simon and Curini, 1998; Froese et al., 1999; Simon et al., 2000). Only a few studies, however, have measured soil pore water pressures in relation to streambank stability and mass failure (Casagli et al., 1999; Simon et al., 1999; Schörghofer et al., 2004).

A mechanism of streambank erosion that has received relatively less attention historically, but whose importance is being increasingly considered, is seepage erosion. Several studies report ground-water sapping, where sapping is defined as the bank collapse resulting from seepage or piping erosion (Laity, 1983; Hagerty, 1991a,b; Worman, 1993; Simon et al., 1999; Fox et al., 2007a,b; Wilson et al., 2007). Seepage erosion occurs when high infiltration rates cause the development of perched water tables above water-restricting horizons in riparian soils (Coates, 1990; Wilson et al., 1991; Jones, 1997) or between layers with contrasting hydraulic conductivity (Hagerty, 1991a,b). As perched water tables rise on these less permeable layers, large hydraulic gradients can initiate toward stream channels, causing fairly rapid subsurface flow (interflow) toward streams. This lateral, subsurface flow can potentially result in erosion of unconsolidated material at the outflow face if the outflow is sufficient to mobilize particles and maintain the velocity necessary to transport mobilized particles away from the site (Higgins, 1982, 1984; McLane, 1984; Dunne, 1990). Seepage flow initiates...
the development and migration of headcuts by liquefaction of soil particles, followed by mass wasting of the streambank by undercutting (Kochel et al., 1985; Ivensen and Major, 1986; Howard and McLane, 1988; Dunne, 1990; Jones, 1997).

Crosta and di Prisco (1999) studied seepage erosion causing liquefaction and rapid slope failures by comparing observed field failure mechanisms and the evolution of the saturated domain using a numerical model. They reported that failure was induced by the three-dimensional development of the saturated domain from a localized source (i.e., point water source from superficial incisions fed by ponded water conditions). Dappporto et al. (2003) investigated the mechanisms of streambank failure and retreat using numerical models to predict changes in soil-water pressure and to analyze the stability of the banks due to variation in the river stage. They demonstrated that the complex interaction between soil-water pressure and the stabilizing confining pressures of river stage plays the primary role in triggering mass failures. Lourenco et al. (2006) examined the relation between soil-water pressure and the failure mode at the interface of two soil layers of different permeability. Although their experiments did not show any clear relation between soil-water pressure and the failure mode, it demonstrated that seepage strongly controlled the failure mechanisms.

More recent field research has highlighted the contribution of seepage erosion to streambank erosion and failure. Field studies of streambank seepage erosion have measured flow rates as high as 1500 L d⁻¹, with sediment concentrations in the range of 10 to 1000 g L⁻¹ (Fox et al., 2007b; Wilson et al., 2007). A detailed knowledge of streambank stratigraphy and hydrologic conditions has been reported to be critical for understanding the occurrence and importance of seepage erosion and sapping (Fox et al., 2007b). Fox et al. (2006) and Wilson et al. (2007) conducted soil lysimeter experiments with repacked streambank soils to quantify the role of soil and hydraulic properties on the seepage erosion process. Chu-Agor et al. (2007) modeled vertical bank angle, layered profile lysimeter experiments to determine the role of seepage undercutting relative to removal of negative soil pore-water pressure in causing large-scale sapping failures. They manually simulated seepage undercutting in the model SLOPE/W by changing the geometry of the seepage layer based on undercutting measurements during the experiments. The modeling technique was able to simulate bank instability by seepage and in general predicted collapse earlier than that observed in the lysimeters. The model also generated slip surfaces that closely matched observed failure planes in the lysimeter experiments. For the conditions modeled, the role of undercutting was more important than removal of negative soil pore-water pressure. Fox et al. (2007a) used the technique of Chu-Agor et al. (2007) to simulate hypothetical seepage undercutting on two Mississippi streambanks. The model predicted a degree of seepage undercutting for sapping that was equivalent to the observed seepage undercut in the field studies of Wilson et al. (2007) and Fox et al. (2007b).

As a first attempt to evaluate the proposed seepage erosion sediment transport model of Fox et al. (2006) and the proposed modeling strategy for seepage undercutting proposed by Chu-Agor et al. (2007), slope destabilization driven by lateral, subsurface flow was studied with uniform profile lysimeter experiments of two contrasting soil types and with banks of varying slope. Fox et al. (2006) limited the derivation of the seepage erosion sediment transport function to a single, loamy sand soil from Little Topashaw Creek (LTC) with vertical (90°) banks. Chu-Agor et al. (2007) only simulated the three-layer, vertical slope, lysimeter experiments that mimicked LTC streambanks. In this research, regression parameters of the seepage erosion sediment transport model were not calibrated so as to evaluate their transferability across noncohesive soil types. This research also investigated whether the slope stability modeling with seepage undercutting could simulate both small-scale bank failures (i.e., sapping events that occur early in the headcut formation due to failure planes on the bank slope) and large-scale bank failures (i.e., sapping events that occur after development of the headcut due to tension crack formation on the bank top surface).

**MATERIALS AND METHODS**

**Lysimeter Experiments**

The lysimeter experiments matched those reported by Fox et al. (2006) and Wilson et al. (2007) and therefore are only summarized briefly. The lysimeter was constructed of Plexiglas and was 100 cm long by 15 cm wide by 100 cm tall (Fig. 1). The experiments were conducted on two different soils: a streambank soil (loamy sand [LS] with 87% sand, 7% clay, and 6% silt and d = 0.25 mm, where d is the 50th percentile grain diameter) from LTC in northern Mississippi, and uniform sieved sand with greater sand content (approximately 95% sand and d = 0.50 mm) and therefore less cohesion than the LTC streambank soil. Bulk samples of the LTC LS streambank soil were obtained approximately 150 cm below ground surface between an overlying silt loam topsoil and a confining clay loam horizon (Fox et al., 2006; Wilson et al., 2007). Pencil-size tensiometers (Soil Measurement Systems, Tucson, AZ) were used to measure soil pore-water pressure to a maximum of 5 cm of H₂O. The soil pore-water pressure inside the tensiometer was monitored with a transducer (Soil Measurement Systems, Tucson, AZ) connected to a datalogger (Campbell Scientific, Logan, UT), which collected data every 15 s. Digital photographs of both the side and front views of the lysimeter were acquired at numerous times during each experiment.

A series of seven lysimeter experiments were performed for the two different sands by varying the bank slope (90° and 60° for the LTC LS to mimic field conditions, and 90, 60, 45, 36, and 26° for the sieved sand to investigate a range of bank angles) with an inflow reservoir water head of 30 cm and bank height of 35 cm (Table 1). The lysimeter was packed by adding disturbed soil samples in 1.0-cm lifts with a bulk density of 1.5 g cm⁻³ and tamping the material using a wooden block. The soil volumetric water content was near field capacity with soil pore-water pressures between ~60 and ~80 cm H₂O (i.e., 5.89 to 7.85 kPa matric suction) based on soil tensiometer readings at the initiation of the experiment. Flow and sediment concentrations were measured at
the outflow flume at discrete time intervals using 1.0-L collection bottles. No attempt was made to quantify the quality (i.e., grain size) of the eroded material. The samples were analyzed to determine discharge, sediment erosion rate, and sediment concentration during that discrete time interval with consideration for the sediment displacement volume in each sample.

Modeling Seepage Erosion

Measured flow and sediment erosion rates were converted into dimensionless discharge and sediment flux following Fox et al. (2006), who derived a simple excess shear stress equation relating dimensionless sediment flux, \( q^*_s \), to a dimensionless shear stress, \( \tau^* \), where the critical shear stress, \( \tau_{cs}^* \), was assumed negligible for the noncohesive seepage layer:

\[
q^*_s = K_{ds} \left( \tau^* - \tau_{cs}^* \right)^\alpha \quad [1]
\]

where

\[
q_s = \frac{q_s}{\sqrt{(s-1)gK_s}} \quad [2]
\]

\[
\tau^* = \frac{C_{q2}^n q \sin(\theta)}{(s-1)aK_s} \quad [3]
\]

where \( K_{ds} \) is the hypothesized seepage erodibility coefficient, \( a \) is the power term (experimentally determined to be 1.04 or approximately 1.0), \( C_{q2}^n \) is an empirical parameter defined by Howard and McLane (1988) that depends on the packing, \( q_s \) is the sediment transport rate, \( q \) is Darcy’s velocity or discharge per unit flow area, \( K_s \) is the saturated hydraulic conductivity, \( n \) is the porosity of the seepage layer, \( g \) is gravitational acceleration, and \( s \) is the ratio of solid to fluid density. The Fox et al. (2006) regression model is similar to an excess shear stress equation commonly used to model fluvial erosion (Hanson and Cook, 1997; Knapen et al., 2007). In fact, the power term, \( a \), is commonly assumed to be 1.0 (Hanson and Cook, 1997; Hanson and Simon, 2001; Knapen et al., 2007).

The dimensionless critical shear stress is based on the theory for initiation of sediment transport by seepage erosion proposed by Howard and McLane (1988), who suggested that surface grains of cohesionless sediment are acted on by gravity, traction, and seepage forces. Observed seepage erosion measurements were nondimensionalized using Eq. [2]. The regression parameters (\( K_{ds} \) and \( a \)) used by Fox et al. (2006) for the LTC streambank lysimeter experiments were then used to predict the dimensionless sediment flux based on input dimensionless discharge. A slight modification was enacted to account for nonvertical banks in the dimensionless shear stress term. The magnitude of the seepage force was reduced by a factor equivalent to \( \sin(\theta) = q/q_s \).

\[
\tau^* = \frac{C_{q2}^n q \sin(\theta)}{(s-1)aK_s} \quad [4]
\]

where \( \theta \) is the bank angle and \( q \) is the seepage vector perpendicular to the bank slope. Theoretically, one would need to measure the \( K_{ds} \) and potentially \( \tau_{cs}^* \) for the particular noncohesive soil undergoing seepage. In this research, there was no attempt to derive an estimate for \( K_{ds} \) for the sieved sand in order to investigate the transferability of this parameter to a unique soil type. The parameter \( \tau_{cs}^* \) was assumed negligible for both soils based on earlier experiments by Fox et al. (2006) with LTC LS and it was expected that, based on a literature review (see Knapen et al., 2007), the \( \tau_{cs}^* \) value of the sieved sand would be less than that of the LTC LS. Linear regression was performed between observed and predicted seepage erosion for each lysimeter experiment and also experiments grouped by soil type.

Predicting Bank Collapse

Following the methodology of Chu-Agor et al. (2007), an integrated finite element model called SEEP/W and a general limit equilibrium bank stability model called SLOPE/W were used to determine if such stability models can capture small-scale and large-scale sapping failures. Seepage flow was modeled using SEEP/W, a finite element model of Richards’ equation for two-dimensional variably saturated flow (Krahn, 2004a). The flow domain was constructed to represent the geometry of the lysimeter and discretized into 25- by 25-mm elements. A potential seepage review boundary condition for all the nodes was assigned at the drainage face. The initial conditions of the models were derived from the initial measured soil pore-water pressure from the lysimeter experiments. Calibration of the SEEP/W model was performed by slightly adjusting the soil hydraulic conductivity, \( K_s \) as well as the van Genuchten water retention curve parameters (\( \alpha \) and \( n \) which are empirical constants affecting the shape of the water retention curve) based on minimizing the root mean square error of predicted vs. measured cumulative discharge.

The model SLOPE/W, a numerical slope stability model (Krahn, 2004b) that uses the theory of limit equilibrium of forces and moments to compute the factor of safety (Fs) against failure, was then used to analyze the stability of the banks. Both SEEP/W and SLOPE/W are integrated codes such that the geometry defined in SEEP/W is used in SLOPE/W. Soil strength parameters in the lysimeter experiment were defined using Mohr-Coulomb’s equation:

\[
s = c' + (\sigma_n - u_n) \tan \phi' \quad [5]
\]

where \( s \) is shear strength, \( c' \) is effective cohesion, \( \phi' \) is the effective angle of internal friction, \( \sigma_n \) is total normal stress, and \( u_n \) is the soil pore-water pressure (Whitlow, 1983; Fredlund and Rahardjo, 1993). In unsaturated soils, matric suction has the effect of increasing the apparent cohesion of the soil, as described by Fredlund and Rahardjo (1993):

\[
s = c' + (\sigma_n - u_n) \tan \phi^b \quad [6]
\]

where \( u_n \) is the soil pore-air pressure and \( \phi^b \) is the angle indicating the rate of increase in the shear strength relative to matric suction and is generally between 10 and 20°. The additional shear strength due to unsaturated conditions was hypothesized to be insignificant in this research because the soil pore-water pressure had reached saturation to near-saturated conditions during most of the seepage erosion process.

The general limit equilibrium method was selected for computing the Fs. This method satisfies both the moment and force equilibrium...
X = ENf(x) [7]

where \( f(x) \) is the specified function, \( \lambda \) is the percentage of the specified function, \( E \) is the interstice normal force, and \( X_n \) and \( X_f \) are the interstice shear forces on either side of a slice. The general limit equilibrium method then uses the following equations of statics to solve for the \( F_s \), where \( W \) is the slice weight; \( D \) is the line load; \( \beta, R, x, f, d, \) and \( \alpha \) are geometric parameters; and \( \alpha' \) is the inclination of the base. The summation of forces in a horizontal direction for each slice is used to compute the interstice normal force, \( E \) (Eq. [7]). This equation is applied in an integration manner across the sliding mass (i.e., from left to right). The summation of forces in a vertical direction for each slice is used to compute the normal force at the base of the slice, \( N \), where \( F \) is either the moment or force equilibrium factor of safety:

\[
N = \frac{W + (X_n - X_f)}{\cos\alpha' + \frac{\sin\alpha' \tan\phi'}{F}} \sum N_f \pm \sum D_d [8]
\]

The summation of moments about a common point for all slices can be rearranged and solved for the moment equilibrium factor of safety, \( F_m \):

\[
F_m = \frac{\sum (\beta R + (N - \alpha') R \tan\phi')}{\sum W - \sum N_f \pm \sum D_d} [9]
\]

The summation of forces in a horizontal direction for all slices gives rise to a force equilibrium factor of safety, \( F_s \):

\[
F_s = \frac{\sum (\beta \cos\alpha' + (N - \alpha') \tan\phi' \cos\alpha')}{N \sin\alpha' - \sum D \cos\omega} [10]
\]

where \( F \) is the \( F_m \) when \( N \) is substituted into Eq. [9] and \( F \) is the \( F_s \) when \( N \) is substituted into Eq. [10]. The relationship between the interstice normal force \( E \) and the interstice \( X \) were both considered and the interstice function was derived from a half-sine function.

The soil pore-water pressure generated from SEEP/W using time steps of 15 s was input into SLOPE/W. The model was then run using the simulated soil pore-water pressure at every time step to determine the effect of the changes on the stability of the slip surface. To model overhanging walls or undercut slopes, such as in the case of undercutting, we followed the procedure of Chu-Agor et al. (2007) in that the geometry of the LTC LS and sieved sand was manually changed based on the dimensions and shape of the undercutting from the digital photographs. The cut was covered with a null region without any soil strength. In SEEP/W, this region was treated as a void in the flow domain. This excluded the weight of the null region in the SLOPE/W analysis.

Two field measurements of cohesion, angle of internal friction, and total unit weight from the LTC streambank site where the lysimeter soil was sampled were performed using a borehole shear test. We assumed that the apparent cohesion measured by the borehole shear test is approximately equal to the effective cohesion for the LS soils. It was not expected that the soils experienced significant drying in the field before the tests and therefore the apparent cohesion was approximately equal to the effective cohesion (Lutenegger and Hallberg, 1981). The LTC LS soil possessed an average cohesion of 1.5 kPa (individual measurements of 1.0 and 2.0 kPa), an average internal angle of friction of 25° (individual measurements of 22 and 29°), and a unit weight of 19 kN/m³ (Chu-Agor et al., 2007). For the sieved sand, these parameters were measured in the laboratory using a direct shear-strength test (Fredlund and Rahardjo, 1993) and resulted in values for cohesion of 0.25 kPa and an internal angle of friction of 35°. The unit weight was approximately 19 kN/m³. The combined SEEP/W and SLOPE/W models were used to investigate the change in the \( F_s \) with and without seepage undercutting with \( \phi' = 0° \). To validate the assumption of negligible influence of matric suction on cohesion, additional SLOPE simulations were run using Eq. [6] with \( \phi' = 20° \), which is the uppermost limit within the range of values reported by Fredlund and Rahardjo (1993).

### RESULTS

**Lysimeter Experiments**

Data from each lysimeter experiment included the total time to bank failure after initiation of seepage flow and the cumulative seepage erosion required to cause bank collapse (Table 2). Experiments with sieved sand with 26 and 36° bank angles did not experience undermining or a bank failure due to seepage undercutting. The seepage zone extended to the surface of the bank slope throughout the entire experiment. Increasing the bank angle to 45°, however, resulted in undermining due to seepage undercutting, with the failure on the bank slope or seepage face. A typical response of the single-layered sieved sand with bank angles >45° to the 30-cm water reservoir inflow head is shown in Fig. 2, including stages of wetting front migration (a–c), seepage erosion (d–g), tension crack formation (h), and undermining or bank collapse (i).

It was interesting to observe the formation of tension cracks in single-layered LTC LS and sieved sand similar to those in the topsoil below the seepage layer in the Fox et al. (2006) lysimeter experiments. The tension crack formed due to the combined forces of reduced cohesion by removal of negative soil pore-water pressure and the induced moment by undercutting. Seepage erosion did not occur as individual motion of sand particles but rather as intermittent mass wastling along bank slope slip surfaces (Fig. 3).

A linear relationship was observed between bank angle and time to failure for the sieved sand experiments. In fact, a linear regression between bank angle and failure time resulted in a trend line with a slope of \(-3.0\ s/°\), intercept of 967 s, and \(R^2\) of 0.61. The relationship between bank angle and the cumulative seepage erosion necessary to cause a bank failure also followed a linear relationship, with a slope of \(-0.05 \ kg/°\), intercept of 6.0 kg, and \(R^2\) of 0.93. For the same bank angle and bank height, the LTC LS required two to three times the amount of seepage...
erosion (i.e., undercutting) to result in bank collapse as the sieved sand (Table 2). The time to failure after seepage flow initiation for the LTC LS was also approximately two to three times higher than the sieved sand. It was hypothesized that these results were directly correlated to the soils’ cohesion.

Modeling Seepage Erosion

For LTC LS and sieved sand banks with angles >45° (i.e., five of the seven experiments), the modified seepage erosion sediment transport model matched observed seepage erosion, with $R^2 > 0.90$ for data before bank collapse (Fig. 4). As expected, the sediment transport model performed better for the LTC LS ($R^2 = 0.98$) than for the sieved sand ($R^2 = 0.65$) because the regression parameters were derived for this soil type. It should be noted, however, that the seepage transport model still adequately predicted ($R^2 > 0.90$) seepage erosion for the less cohesive sieved sand for bank angles >45° (Table 3).

Model predictions for the sieved sand deviated further from observations as slopes declined to 26 and 36°. Lysimeter experiments with 26 and 36° bank angles lacked the occurrence of a well-defined bank undermining or collapse by seepage undercutting and therefore behaved different than experiments with bank angles >45°. The 26 and 36° bank angle experiments consisted of small-scale undermining events that migrated laterally up the bank slope and prevented a single, large-scale bank failure. For the 45° bank angle, the failure that occurred during the experiment occurred on the bank slope rather than the bank top surface. Therefore, the seepage erosion sediment transport model underpredicted the seepage erosion by not accounting for long-term small-scale undermining events, which correspondingly resulted in larger observed sediment transport during the later stages of the experiments (Fig. 4). Observed seepage erosion for experiments with 45, 60, and 90° banks gradually increased throughout the experiment, while seepage erosion for the 26 and 36° banks fluctuated during the later stages of the experiments. Such small-scale undermining events may limit the applicability of proposed sediment transport functions to time periods before tension crack formation and undermining on the bank slope.

Predicting Bank Collapse

The SEEP/W flow code predicted cumulative discharge in both the LTC LS and sieved sand experiments with minimal calibration ($K_s = 429$ cm/d, $\alpha = 0.02$ cm$^{-1}$, $n = 3$, $\theta_s = 0.03$, and $\theta_r = 0.40$ for the LTC LS and $K_s = 744$ cm/d, $\alpha = 0.13$ cm$^{-1}$, $n = 5$, $\theta_s = 0.03$, and $\theta_r = 0.40$ for the sieved sand, where $\theta_s$ and $\theta_r$ are the residual and saturated volumetric water contents, respectively). For all lysimeter experiments, the slope, intercept, and $R^2$ values of the linear regression between predicted and observed cumulative discharge
were >0.95, <10.0 cm³, and >0.98, respectively. Calibration parameters fell well within the range of measurements at the LTC site as reported by Wilson et al. (2007) and Fox et al. (2006). The soil pore-water pressure data from SEEP/W was fed into the SLOPE/W model for each lysimeter experiment.

The additional consideration of matric suction on the cohesion of the soil was only significant for these specific lysimeter experiments at the beginning of the experiment, when soil pore-water pressures were between field capacity and residual water content (Table 4). The SLOPE/W simulations with \( \phi^b = 20^\circ \) did not significantly impact the resulting Fs (i.e., <5% increase) once seepage undercutting become active (Table 4). Once the wetting front reached the outflow face, soil tensiometers and SEEP/W simulations suggested positive pore-water pressures throughout much of the lysimeter. Matric suction effects have been recognized to be an important factor, however, in controlling failure mechanisms in the field, where much of the bank may be above the water table (Rinaldi and Casagli, 1999; Simon et al., 2000).

The SLOPE/W model predicted bank collapse sooner than observed in the lysimeter, probably due to compression forces from the side walls of the lysimeter, for the 60 and 90° bank angles (for both the sieved sand and the LTC LS), with an example shown in Fig. 5. The SLOPE/W model did not predict failure for the 26 and 36° sieved sand experiments, and predicted failure much later than observed (i.e., requiring a greater degree of seepage undercutting) for the 45° angle.

Without considering seepage undercutting, increasing the soil pore-water pressure reduced the stability of the banks due to the reduction in the cohesion of the soil. For the 60 and 90° LTC banks, the Fs decreased by 7.2% (from 1.67 to 1.55) and 1.5% (from 1.15 to 1.13), respectively, with \( \phi^b = 0^\circ \). For the 90 and 60° sieved sand banks, the model predicted a decrease of 16.5 and 11.6%, respectively, due to the arrival of the wetting front at the bank face. The decrease in the factor of safety (initial and final Fs) for the 45° angle.

The interrelationship between seepage undercutting and increased soil pore-water pressure is important for predicting the large-scale sapping failures observed in the lysimeter experiments with bank angles >45°. Seepage undercutting continued to decrease the stability of the banks beyond soil pore-water pressure effects. For example, seepage undercutting reduced the simulated Fs from 0.78 at initiation of seepage erosion to 0.27 at the time of observed bank collapse for the 60° sieved sand experiment, assuming \( \phi^b = 0^\circ \) (Fig. 5).

**DISCUSSION**

Seepage erosion and undercutting has not been thoroughly investigated as a mechanism of streambank instability, even though it is noted to occur along streambanks in numerous locations (i.e., Crosta and di Prisco, 1999; Dapporto et al., 2003). Grissinger (1996) explicitly pointed out that bank failure is a three-stage process, with the first stage being prefailure conditioning, which is largely driven by seepage flow, freeze–thaw, wetting–drying, and bank profile water content. Similar to field observations, groundwater seepage served as a critical precursor to bank failure by forming headcuts (i.e., undercutting) and increasing the bank profile water content in these two-dimensional lysimeter experiments with LTC LS and sieved sand banks.

These lysimeter experiments further verified that an excess shear stress equation with a modified erodibility coefficient for seepage and negligible critical shear stress adequately represents the

Table 3. Linear regression results for measured vs. predicted dimensionless seepage erosion for Little Topashaw Creek (LTC) loamy sand and sieved sand lysimeter experiments.

<table>
<thead>
<tr>
<th>Soil type</th>
<th>Water head in inflow reservoir</th>
<th>Bank height</th>
<th>Bank angle</th>
<th>Slope</th>
<th>Intercept</th>
<th>( R^2 )</th>
</tr>
</thead>
<tbody>
<tr>
<td>LTC loamy sand</td>
<td>30 cm</td>
<td>35 cm</td>
<td>90°</td>
<td>0.96</td>
<td>0.00</td>
<td>0.99</td>
</tr>
<tr>
<td></td>
<td>30 cm</td>
<td>35 cm</td>
<td>60°</td>
<td>0.80</td>
<td>0.01</td>
<td>0.99</td>
</tr>
<tr>
<td></td>
<td>all experiments</td>
<td></td>
<td></td>
<td>0.84</td>
<td>0.01</td>
<td>0.98</td>
</tr>
<tr>
<td>Sieved sand</td>
<td>30 cm</td>
<td>35 cm</td>
<td>90°</td>
<td>0.65</td>
<td>0.11</td>
<td>0.90</td>
</tr>
<tr>
<td></td>
<td>30 cm</td>
<td>35 cm</td>
<td>60°</td>
<td>1.39</td>
<td>0.02</td>
<td>0.91</td>
</tr>
<tr>
<td></td>
<td>30 cm</td>
<td>35 cm</td>
<td>45°</td>
<td>1.05</td>
<td>0.10</td>
<td>0.91</td>
</tr>
<tr>
<td></td>
<td>30 cm</td>
<td>35 cm</td>
<td>36°</td>
<td>0.74</td>
<td>0.09</td>
<td>0.81</td>
</tr>
<tr>
<td></td>
<td>30 cm</td>
<td>35 cm</td>
<td>26°</td>
<td>0.37</td>
<td>0.14</td>
<td>0.47</td>
</tr>
<tr>
<td></td>
<td>all experiments</td>
<td></td>
<td></td>
<td>0.95</td>
<td>0.06</td>
<td>0.65</td>
</tr>
</tbody>
</table>
relationship between seepage discharge and sediment transport before bank collapse. Using the erodibility coefficient derived for the field streambank soil appeared appropriate for the less cohesive sieved sand, suggesting that the seepage erodibility parameter may be transferable. The advantage of such excess shear stress formulations, as opposed to the more theoretically based sediment transport functions of Howard and McLane (1988), is that they may be able to be used simultaneously with surface runoff shear stress equations through modification of the erodibility coefficient and critical shear stress. Owoputi and Stolte (2001) performed experiments with both seepage and runoff and indicated that seepage can influence the rate of erosion. Future work should be aimed at deriving both the erodibility parameter and the critical shear stress for a range of soils with different textures and cohesion and with and without seepage and runoff. Streambank failures are due to a combination of both surface and subaerial processes (Simon et al., 1999, 2000; Dapporto et al., 2003).

Previous research has indicated the need to incorporate bank stability models with variably saturated flow codes for predicting the influence of unsaturated and saturated soil pore-water pressures on stability (Crosta and di Prisco, 1999; Dapporto et al., 2003; Rinaldi et al., 2004). Much of this work, however, has focused on the influence of pore-water pressure on soil shear strength (Lourenco et al., 2006), not on the instability by seepage undercutting. In areas where field conditions suggest convergence of groundwater flow from perched water tables, such as in the cases documented by Crosta and di Prisco (1999), Fox et al. (2007b), and Wilson et al. (2007), instability can be caused by seepage undercutting.

In this research, the integrated flow (SEEP/W) and bank stability (SLOPE/W) models, with the seepage undercutting procedure proposed by Chu-Agor et al. (2007), adequately represented large-scale bank collapse by seepage undercutting and increases in soil pore-water pressure for banks with >45° angles. The role of seepage undercutting was just as important as increased soil pore-water pressure in causing bank failures in these lysimeter experiments. These results further support the hypotheses of Fox et al. (2007b) and Wilson et al. (2007) that the relative importance of groundwater erosion mechanisms (i.e., seepage undercutting and increases in soil pore-water pressure) is dependent on site-specific streambank stratigraphy and hydrologic conditions.

The use of a limit equilibrium approach may be a barrier in the use of such models for predicting small-scale sapping events observed early in the headcut formation. The SLOPE/W stability equations are based on the limit equilibrium method and the slip surface generated by the model is a circular arc in cross section. This arc surface is used to avoid analytical routines of considerable complexity. More complex slip surfaces can be modeled, such as log-spiral or irregular shapes; however, the analysis tends to be long and tedious (Whitlow, 1983). For the one-layer soil experiments considered here, the base of the lysimeter serves as the restricting layer and SLOPE/W always constructed the slip surface at the base. The arc generally extended to the bank top surface as opposed to the bank slope, i.e., the location of failure planes for small-scale sapping. Small-scale sapping events are the only failures that occurred in lysimeter experiments with bank angles <45°; i.e., failure along a circular slip surface did not occur in the lysimeter experiments with 26 and 36° banks. This was accurately predicted by SLOPE/W based on limit equilibrium theory. More

Table 4. Factor of safety (Fs) predicted by SLOPE/W model for cases where there is no effect of matric suction on the cohesion of the lysimeter soils (i.e., $\phi^b = 0^\circ$) and considering partial saturation effects on the soil strength (i.e., $\phi^b = 20^\circ$, which is a typical value reported by Fredlund and Rahardjo, 1993).

<table>
<thead>
<tr>
<th>Soil type</th>
<th>Water head in inflow reservoir (cm)</th>
<th>Bank height (cm)</th>
<th>Bank angle (°)</th>
<th>Fs at initial condition</th>
<th>Fs at seepage initiation</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>$\phi^b = 0^\circ$</td>
<td>$\phi^b = 20^\circ$</td>
<td>$\phi^b = 0^\circ$</td>
</tr>
<tr>
<td>Loamy sand</td>
<td>30</td>
<td>35</td>
<td>90</td>
<td>1.15</td>
<td>1.36</td>
</tr>
<tr>
<td></td>
<td>30</td>
<td>35</td>
<td>60</td>
<td>1.67</td>
<td>2.13</td>
</tr>
<tr>
<td>Sieved sand</td>
<td>30</td>
<td>35</td>
<td>90</td>
<td>0.79</td>
<td>1.08</td>
</tr>
<tr>
<td></td>
<td>30</td>
<td>35</td>
<td>60</td>
<td>0.88</td>
<td>1.54</td>
</tr>
<tr>
<td></td>
<td>30</td>
<td>35</td>
<td>45</td>
<td>1.22</td>
<td>1.81</td>
</tr>
<tr>
<td></td>
<td>30</td>
<td>35</td>
<td>36</td>
<td>1.76</td>
<td>2.43</td>
</tr>
<tr>
<td></td>
<td>30</td>
<td>35</td>
<td>26</td>
<td>2.07</td>
<td>2.77</td>
</tr>
</tbody>
</table>

Fig. 5. The SLOPE/W simulated factor of safety (Fs) for the 60° sieved sand bank angle and pictures demonstrating the wetting front and depth of undercutting at each stage of the lysimeter experiment, with no effect of matric suction on shear strength ($\phi^b = 0^\circ$).
advanced and analytically complex slip surfaces are required to detect small-scale sapping failures.

The use of the procedure of Chu-Agor et al. (2007) to incorporate seepage undercutting remains a static analysis. The user must physically manipulate the geometry of the bank in both the flow and stability codes based on a priori knowledge of the distance or depth of seepage undercutting. For field applications, users would need to physically measure the depth of seepage undercutting and incorporate this geometry into the stability models. Future research should be aimed at incorporating the dynamic process of seepage undercutting into streambank stability models, but this requires structured feedback between bank stability, variably saturated flow codes, and a seepage sediment transport function. Needed is more detailed knowledge of the applicability of shear stress equations for seepage erosion and the relationships between seepage sediment transport and bank geometry changes. Such improved bank stability models will better consider site-specific bank failure processes, which Grissinger and Little (1986) and Grissinger (1996) suggested as being critical for gully and stream channel rehabilitation.

CONCLUSIONS

The lysimeter experiments demonstrated a three-stage erosion process: (i) seepage erosion; (ii) tension crack formation on either the bank slope (referred to as undermining) or the bank top surface; and (iii) bank collapse (sapping). A slight modification of an existing sediment transport model adequately simulated seepage erosion in lysimeter experiments (especially for bank angles >45°) with both noncohesive soils without modifying the seepage parameters of the excess shear stress equation. These results suggest that the seepage erodibility parameter may be transferable for noncohesive soils with similar packing. The transport model was limited to predicting seepage erosion before small-scale bank failures on the bank slope. Integrated flow and bank stability models with the incorporation of bank geometry changes by seepage undercutting adequately represented large-scale bank collapse for banks with >45° angles. For these lysimeter experiments, the role of seepage undercutting was equivalent to slightly greater than the role of increased soil pore-water pressure in leading to bank failure. The stability model could not predict small-scale sapping failures along the bank slope, which were common in lysimeter experiments with slopes <45°, due to the use of cylindrical or circular arc (in cross section) slip surfaces. More complex stability approaches are needed to capture bank slope undermining.

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