Low wear metal sliding electrical contacts at high current density

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Operation of a low wear (2 × 10⁻⁵ mm³/(N-m)), low contact resistance copper sliding electrical contact was demonstrated. The wear rate of a lightly loaded copper–beryllium metal fiber sliding on a polished copper counterface was insensitive to (DC) current density values as great as 440 A/cm² (in a brush positive or anodic configuration). Low wear and relatively low friction (μ ~ 0.2 to 0.3) was achieved by operating the contact immersed in a liquid medium consisting of a hydrofluoroether with helium cover gas, inhibiting oxidation and providing cooling of the contact. Similar experiments performed in liquid mediums of ultrapure water and dilute (3%) hydrogen peroxide show an order of magnitude increase in wear rate and provide further insight on the role of electrochemically enhanced oxidation and the degraded contact resistance and tribological behavior of non-noble sliding electrical contacts in general.

In contrast to high current density sliding in hydrofluoroether, an order of magnitude greater wear rate was observed for similar sliding conditions in hydrogen peroxide or water without the aid of externally supplied electric potential. A conceptual model is proposed correlating the rate of brush wear to fatigue strength and electrochemically enhanced oxidation as a result of high current density transport through the contact. A mathematical expression was derived to calculate the approximate wear volume of a single fiber laterally contacting a slip-ring, based on direct measurement of the wear scar geometry.

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1. Introduction

In a 1979 review paper on the state of the art in current collection technology McNab [1] suggested that construction of metal fiber brushes is one of the primary technical challenges in the design of modern current collectors capable of unprecedented performance with regard to high current density and low wear rate. The term brush as referring to a sliding electrical contact has its origin in some of the first commercial designs of electric motors where current collection was achieved by means of bundled copper fibers resembling painter’s brushes [1]. Early current collector designs implementing metal–metal contacts were plagued with high friction, wear, and heat generation mainly attributed to electrochemical oxidation and arcing. A paradigm shift in current collection technology occurred at the end of the 19th century with the invention of the carbon brush. Further improvements in carbon brush technology came with the invention of electrographite and metal–graphite composite brushes, both of which are still used today in a host of applications like fuel pumps, power generation turbines, and electric rail transportation. The main disadvantages of carbon brushes remain the low system compliance of monolithic brushes and the inherently high resistance to electrical conduction resulting in generation of waste heat, in both cases in contrast to metal multi-fiber brushes. Also, monolithic brushes exhibit a limited ability to track typically rough slip–ring surfaces, particularly at high speed [2].

Kuhlmann–Wilsdorf estimated that typical graphite brushes sliding on metal slip–rings will exhibit an order of magnitude higher drop in electrical potential compared to metal–metal sliding electrical contacts [3].

As described in detail by authors like Holm [4], the same surface films (oxides, moisture, and carbonaceous compounds) responsible for desirable friction and wear behavior of metal sliding electrical contacts play a detrimental role on contact resistance, in the form of film resistance. One of the defining features of metal sliding electrical contacts is that for all practical purposes the overall system performance of a large scale device will be intimately tied to the few hundred nanometers describing the interface of the sliding contact.

One of the surprising consequences of this research has been the realization that under the right conditions metal sliding electrical contacts are quite robust and capable of sustaining extreme density of current transport with minimal energy loss. The ability to sustain ten or one-hundred fold greater current density than a carbon brush while maintaining reasonably low wear rates with relatively inexpensive materials (e.g. copper wire) may facilitate more compact electrical machinery.

This work presents experimental research highlighting the critical role of interfacial phenomena and electrochemistry on the performance of self-mated copper sliding electrical contacts, some...
features of which are generally applicable to other non-noble metals. Friction, wear, and contact resistance measurements were used as a metric of performance for the case of copper sliding electrical contacts. Sliding contacts were immersed in the following prescribed liquid mediums and cover gases:

1. a dilute (3%) hydrogen peroxide (H₂O₂) and water solution exposed to laboratory air,
2. ultrapure water exposed to laboratory air,
3. And a hydrofluoroether with an inert cover gas (helium was chosen over heavier and less expensive inert gases due to the higher rate of diffusion of He into HFE, minimizing the time needed to displace trace amounts of dissolved oxygen as a result of exposure to lab air during storage).

These fluids were selected so as to possess certain properties in common, such as viscosity near room temperature and specific heat capacity (the ability to cool the contact due to Joule heating when transporting 100 s A/cm²). By implementing materials like copper (with a relatively high thermal diffusivity) it is possible to maintain a cool contact while at elevated current density, minimizing the role of temperature on oxidation kinetics and highlighting the role of electrochemical processes on the tribological performance of the contact.

More specifically, while maintaining otherwise similar material and environmental properties it was shown that a copper sliding electrical contact exhibits wear rates and contact resistances that are insensitive to the density of current transport up to values as high as 400 A/cm² for sliding distances exceeding 10,000 cycles (~1000 m, corresponding to about 24 h of sustained operation) when sliding in an oxidation inhibiting environment (in this case a hydrofluoroether). It was also demonstrated that enhanced oxidation, achieved chemically using a dilute hydrogen peroxide solution or electrochemically as a result of current transport while immersed in water, resulted in order of magnitude larger wear rates. Also, for the case of sliding in water with current transport, the presence of current flow immediately led to deterioration in contact resistance, in stark contrast to the apparently insensitive contact resistance response of the same materials operated in an oxidation inhibiting environment under otherwise equivalent test conditions. This insensitivity provides indirect evidence that deterioration in the performance of a sliding electrical contact is directly correlated to enhanced oxidation, a hypothesis first proposed by Boyer et al. [5]. A more in-depth investigation attempting to quantify the degree of enhanced oxidation remains pending. For a more detailed investigation of copper oxidation kinetics in dilute hydrogen peroxide see DeNardis et al. [6]. Details of copper oxidation in water may be found in M. Pourbaix’s “Atlas of Electrochemical Equilibria in Aqueous Solutions” [7].

This work also presents an alternate approach to the investigation of the tribological performance of metal sliding electrical contacts. A single fiber contact removes many of the complexities and uncertainties characteristic of multi-fiber brush experiments typically found in the literature [1,3,8–14] where rough estimates must be made on the number of fibers in contact, affecting the real distribution of mechanical and electrical load and confounding analyses of tribological performance. In the method described here the wear scar is directly observed and its area quantified, making analysis of nominal contact stress and current density. Similarly, this method makes it possible to measure wear after sliding hours rather than weeks, typically a necessity in larger scale experiments due to the inherently low wear rates and high uncertainty (due to factors such as splaying of multi-fiber brushes) [9]. A mathematical expression was derived to estimate the wear volume of a metal fiber laterally contacting a slip-ring, based on direct observation and measurement of the wear area. The ability to estimate wear and, to immerse the contact, also adds a greater level
of variability and control in the continuing effort to understand the complex interactions that define these types of contacts.

2. Description of experiments

2.1. Tribometer description and sample preparation

Friction experiments were performed on a custom built friction testing apparatus (tribometer) shown in Fig. 1. The tribometer operates in a pin-on-disk configuration. The pin consisted of a single bent copper–beryllium fiber (UNS C17200, full hard from Little Falls Alloys, USA) with a nominal diameter (2r) of 120 μm (see Fig. 2 for illustration of geometry). The bend radius (R) varied in the range of 2.95–3.05 mm due to spring-back of the coiled fiber. The counterface was the flat end of a polished 20 mm diameter, 30 mm long copper cylinder (UNS C11000 from McMaster-Carr, USA). The copper cylinder was press-fit onto a polymer cup intended to hold a liquid volume of 12.5 cm³, submerging the counterface and bottom half of the fiber. The polymer cup was machined from virgin polytetrafluoroethylene (PTFE) rod stock. The counterface was mounted directly to the shaft of a stepper drive motor (Schneider Electric Motion, USA, MDrive 17). A fixture consisting of a pair of aluminum disks sits between the counterface and motor shaft, with a thin foam spacer and three equally spaced screws along the circumference fastening the two disks together. The fixture was adjusted manually using the three screws until the motor shaft and counterface axes were aligned, eliminating load fluctuations due to disk runout. Runout resulted in a normal force fluctuation approximately ±14% of the target load (e.g., for a 0.50 mN normal force the load fluctuated sinusoidally from 0.43 to 0.57 mN). Prior to every test, the running surface of the copper counterface was wet sanded with progressively finer sandpaper down to 1000 grit, followed by wet lapping with 1 μm Al₂O₃–water slurry on a lapping felt pad. The polished cylinders were promptly rinsed with laboratory grade isopropyl alcohol and imaged using a scanning white light interferometer (SWLI; ZYGO NewView 5032) to evaluate the surface finish. The average (Rₛ) and root-mean-square (Rₚ) surface roughness were calculated based on measured values for three arbitrarily chosen representative scans (365 × 365 μm² areas) of the running surface for each counterface, and were in all cases Rₛ ~ 15 nm and Rₚ ~ 20 ±10 nm, respectively. Testing was promptly initiated after imaging. Tests were periodically interrupted to image the fiber and assess the volume loss as a function of sliding distance, requiring a removal from the prescribed environment lasting approximately 15 min.

2.2. Force measurement and friction coefficient

The pin (metal fiber) was coiled and epoxied to a polymer cylinder with a small hole for the fiber, made of polyetheretherketone (PEEK), meant to electrically insulate the fiber from the tribometer frame. Refer to Fig. 1(c) for illustration. The PEEK cylinder is epoxied to the tip of a cantilevered double-leaf flexure. Contact between pin and counterface is achieved by vertically displacing the instrumented head, consisting of the flexure cantilever and two capacitance probes. Friction coefficients are calculated from deflections of the cantilever tip as a response to normal and traction forces on the pin, measured using two capacitance probes. A detailed description of force measurement and application using capacitance probes and leaf-flexure cantilevers can be found in [15–17]. Based on the displacement measurement limits of the capacitance probes the range of measurable normal and traction forces was on the order of ±2 mN, with an optimal resolution (based on a 15 nm probe resolution and neglecting other sources of error) of ~0.6 μN. In practice, typical fluctuations in load measurement when the pin was brought into contact with a stationary disk were on the order of 5 μN.

2.3. Electrical current source and setup

In some experiments a direct (DC) electrical current was applied through the sliding contact. The metal fiber was always configured
as the positive element (anode). Current was supplied using a digital multimeter and power supply (Keithley 2400 Sourcemeter) in a constant voltage output mode (current is allowed to vary as a function of the changing contact resistance). The line resistance of the circuit was calibrated to be approximately 100 Ω. Reported contact resistance values are calculated using Ohm’s Law, based on the measurements of current flow through and voltage drop across the contact. The voltage drop is measured at the two soldered connections near the contact, one near the end of the pick-up brush and one near the metal fiber (visible in Fig. 1(c) where the current delivery and voltage tap wires are soldered to the coiled copper wire in series with the metal fiber). This measurement type is generally referred to as a four-point probe, where two wires deliver electrical current by means of a power supply (the circuit through which the current flow is being measured) and two wire taps are placed as close to the sliding contact as possible so as to measure mainly the voltage drop through the sliding contact (minimizing the contribution of bulk material). A pick-up brush is necessary to complete the electrical circuit through the rotating slip-ring. This setup implies that a part of the contact resistance will be due to the bulk resistance of the copper wire coil, the length of copper–beryllium fiber, and the length of the multi-fiber copper pick-up brush. The contribution to contact resistance is estimated to be less than about 0.5 Ω, a negligible fraction of the reported values for contact resistance.

2.4. Environment: liquid medium and cover gas

For the experiments described here the sliding contact was operated immersed in a prescribed liquid medium. The tribometer was operated either exposed to laboratory air (approximately 35% relative humidity and 21 °C) or inside a gas tight chamber with a continuous flow of high purity, laboratory grade helium. Three different liquid mediums were tested: (1) ultrapure (18 MΩ cm) water, (2) a 3% hydrogen peroxide and water solution, and (3) a hydrofluoroether engineered fluid (3M™, Novec™ 7500). A helium cover gas was only used for case (3) where it was desirable to minimize oxidation of the metal fiber and counterface due to oxygen and water dissolved in the HFE during storage and exposure to lab air. The possibility of hydroplaning of the immersed metal fiber at 10 mm/s sliding speed was negligible; observed friction coefficients are indicative of sliding in the boundary lubrication regime.

2.5. Data acquisition

Data was acquired using a desktop PC with custom LabVIEW software and a National Instruments DAQ card (PCI-6229). Analog voltages are supplied by the capacitance probe controller as inputs and are supplied to the stepper motor controller as output via an NI SCB-68 connector block. The analog signals sent from the capacitance probe controller for each probe is acquired at 10 kHz and averaged into a single data point per second. Each data point is plotted and saved to a tab delimited text file, and corresponds to the average of 10,000 discreet measurements. The resolution of the acquisition card is 16-bit, so for a ±10 V range of input there is a maximum theoretical resolution of 0.3 μV. For the known stiffness of the cantilevers used in this study this corresponds to an effective normal force resolution limit of 61 nN, or several orders of magnitude smaller than the measured values. The custom software calculates friction coefficient based on the capacitance probe signals, plots and saves data, and communicates with the motor to set RPM and start/stop motion.

### Table 1

<table>
<thead>
<tr>
<th>Liquid/cover gas</th>
<th>( K_{\text{vol}} ) (mm³/(N·m))</th>
<th>( f ) (A/cm²)</th>
<th>( \mu )</th>
<th>( R_c ) (Ω)</th>
</tr>
</thead>
<tbody>
<tr>
<td>3% H₂O₂/lab air</td>
<td>2.9 × 10⁻⁴</td>
<td>0</td>
<td>0.49</td>
<td>-</td>
</tr>
<tr>
<td>Ultrapure water/lab air</td>
<td>2.6 × 10⁻⁴</td>
<td>265</td>
<td>0.1–0.4⁴</td>
<td>68</td>
</tr>
<tr>
<td>Ultrapure water/lab air</td>
<td>1.2 × 10⁻⁴</td>
<td>0</td>
<td>0.46</td>
<td>-</td>
</tr>
<tr>
<td>Hydrofluoroether/helium</td>
<td>2.2 × 10⁻⁵</td>
<td>88</td>
<td>0.15</td>
<td>26.5⁴</td>
</tr>
<tr>
<td>Hydrofluoroether/helium</td>
<td>1.8 × 10⁻⁵</td>
<td>0</td>
<td>0.31</td>
<td>-</td>
</tr>
<tr>
<td>Hydrofluoroether/helium</td>
<td>1.7 × 10⁻⁵</td>
<td>442</td>
<td>0.27</td>
<td>8.2</td>
</tr>
</tbody>
</table>

* Nominal current density (\( f \)) is calculated based on the cross-sectional area of the 120 μm diameter metal fiber (\( \sim 1.13 \times 10^{-7} \text{cm}^2 \)).

### 3. Results

#### 3.1. Metal fiber wear rate

Wear was assessed by measuring the volume removal rate on the metal fiber as a function of sliding distance. The specific wear rate (\( K_{\text{vol}} \) in mm³/(N·m), Eq. (1)) or volumetric wear rate is reported for these experiments.

\[
K_{\text{vol}} = \frac{V_{\text{loss}}}{F_s \delta} \text{ (mm}^3/\text{N-m)}
\]

The specific wear rate is the ratio of volume loss (\( V_{\text{loss}} \)) and the product of sliding distance (\( \delta \)) and applied normal force (\( F_s \)). The average normal force value of 0.5 mN was used to calculate the wear rate. The sliding distance is calculated based on the calibrated RPM and wear track radius (a nominal wear track radius of 8 mm and a linear sliding speed of 10 mm/s were used, upon a freshly prepared copper counterface, for all experiments). The measurement of wear volume on the toroid fiber is based on the geometry of the scar, evaluated using SWLI microscopy micrographs such as the example shown in Fig. 2. An expression was derived for calculating the approximate wear volume on the surface of a thin diameter (2r) fiber bent into a circular arc (a toroid) of measured radius (\( R \)). The worn volume is effectively the segment of a toroid, producing an elliptical flat spot on the side of the metal fiber with a measured semi-major radius (\( a \)). The detailed derivation of the expression for the volume of a toroidal segment (Eq. (2)) is provided in the appendix.

\[
V_{\text{loss}} = \frac{\pi a^2}{4} \sqrt{\frac{T}{R}}
\]

The metal fibers had a variation in bend radius (\( R \)) between 2.95 and 3.05 mm which was recorded for each experiment, and a nominal fiber radius (\( r \)) of 60 μm. Wear rate data is shown in Fig. 3. Refer to Table 1 for a summary of test results. The following is a list of notable observations pertaining to the wear data of Fig. 3.

1. The wear rate of the metal fiber sliding immersed in hydrofluoroether was approximately \( 2 \times 10^{-3} \text{mm}^3/(\text{N·m}) \) and was insensitive to the sustained passage of DC electrical current through the junction up to a density of 442 A/cm² based on the metal fiber cross-sectional area (or more than 2300 A/cm² based on the worn contact area).
2. The wear rate of the metal fiber sliding immersed in 3% H₂O₂ was an order of magnitude greater, at approximately \( 3 \times 10^{-4} \text{mm}^3/(\text{N·m}) \).
3. The wear rate of the metal fiber sliding immersed in ultrapure water without electric current flow was also an order of
magnitude higher than the case of hydrofluoroether, and was sensitive to the addition of an electrical current load through the contact. Upon applying a 5 V DC supply to the circuit (consisting of a 100 Ω line resistance and the contact resistance, in series) the wear rate was comparable. Although a sharp increase in contact resistance from 10 Ω to 68 Ω was observed, along with a decrease in friction coefficient, it was not possible to determine if there was a change in wear rate during the period of sliding with current due to the relatively high uncertainty in measurement of the wear scar area (the contact area and reflectivity were irregular, in contrast to well defined, flat, more reflective wear scars observed after sliding in HFE). The friction coefficient showed a gradual recovery to its original value after a period of sliding without current.

3.2. Friction and contact resistance

Friction coefficient and contact resistance results are shown in Fig. 4 and are again summarized in Table 1. The plots in Fig. 4 represent full data sets. For a linear sliding speed of 10 mm/s the total duration of a test lasting 100/200/1000 m of sliding corresponds to 2.8/5.5/27.8 h of test duration. Average friction coefficient and contact resistance values shown in Fig. 4 are calculated for periods of relatively steady-state operation, as illustrated in the figure.

For the case of sliding while immersed in ultrapure water the friction coefficient gradually increases during the first 100 m of sliding (~2.8 h). Upon applying a DC current through the contact there is a sharp drop in friction coefficient and increase in contact resistance, attributed to an electrochemical enhanced process at the interface. Contact resistance at the onset of electrical current flow was similar in magnitude to the steady-state value observed for the case of hydrofluoroether particularly following the brief exposure of the pin and counterface to lab air during imaging (allowing natural oxidation to occur on otherwise oxide free surfaces operating in hydrofluoroether). A nearly order of magnitude higher contact resistance was observed after prolonged operation in water. This is in contrast to operation in hydrofluoroether. Friction coefficients in hydrofluoroether were approximately 0.27 (with a standard deviation of 0.06), and in oxidative mediums (ultrapure water and 3% H₂O₂) approximately 0.46 (with a standard deviation of 0.03).

3.3. Quantifying wear rate uncertainty

The uncertainty in wear rate $u(K_{vol})$ is a function of the uncertainties in measured volume loss $u(V_{loss})$, normal force $u(F_N)$, and sliding distance $u(s)$. As described in greater detail by Burris et al. [18] and Argibay et al. [9] the uncertainty in volume is typically the primary source of uncertainty in calculation of specific wear rates for these experimental setups. Eq. (4) is an expression of the uncertainty in specific wear rate. The derivation is similar to that appearing in Burris et al. [18] and Schmitz et al. [19], and assumes negligible uncertainty in the measurement of the wear scar semi-major radius $r$ and $R$.

$$u(V_{loss}) \approx \left[ \frac{\pi a^2}{R} \sqrt{\frac{F}{R}} \right] u(a)$$

A conservative approximation of the uncertainty in measurement of the wear scar semi-major radius $u(a) \sim 5 \mu m$ is assumed for all measurements. This approximation is based on the error in estimating the boundary of the wear scar on a topographical white light interference micrograph. For the metal fiber under consideration ($r = 60 \mu m, R = 3.0 mm$) and a value of $a$ of 100 μm, the volume loss based on Eq. (2) is $V_{loss} \sim 3.7 \times 10^{-6}$ mm³ and the uncertainty in volume loss based on Eq. (3) is $u(V_{loss}) \sim 0.74 \times 10^{-6}$ mm³.
4. Discussion

4.1. Role of oxidation on the wear rate of metal sliding electrical contacts

Previous studies by Reichner et al. [12,13] and Boyer et al. [5,20], among others, describe a phenomenon observed in metal sliding electrical contacts with a high density of DC electrical current transport where the positive (anodic) brush wears at a higher rate than the negative (cathodic) brush while operating in humidified inert cover gases. Boyer et al. [5] proposed a theory based on a modification of the Cabrera–Mott oxidation model [21,22] whereby the electric field through the copper oxide surface films, generated due to large current driving potentials, may be sufficient to surpass the chemical potential driving ion diffusion through the oxide layer, resulting in an enhanced (or inhibited, depending on polarity) growth rate of the oxide. Boyer et al. postulate [5] that for a positive brush (anodic) the oxidation growth rate is enhanced, and for a negative brush (cathodic) it is inhibited, often by as much as an order of magnitude with respect to the natural, chemical oxidation rate in a given oxidizing environment. The model is sensitive to localized temperature, hinting at the importance of effective cooling and frictional heating due to high contact pressure and/or sliding speed.

The observed trends in contact resistance measured for the copper systems evaluated in this study, contrasting between measurements of copper sliding electrical contacts operating immersed in water or a hydrofluoroether, appears to support the proposed role of oxidation kinetics on the wear rate of sliding metal contacts constructed from non-noble materials. There was negligible change in the contact resistance of copper sliding electrical contacts sliding in an oxidation inhibiting, thermally conductive, electrically insulating medium (a hydrofluoroether with a helium cover gas). A two-fold increase in the wear rate and a gradual increase in contact resistance with time were observed for the contact immersed in water when an electric potential was applied to the contact (constant 5 VDC through a total circuit resistance consisting of a 100Ω line resistance + the measured contact resistance, resulting in a maximum of 30–50 mA of current flow, depending on the environment). Contacts immersed in dilute (3%) hydrogen peroxide solution, a medium known to enhance copper oxidation [6], wore at a rate approximately equal (or slightly greater) to the case of immersion in ultra-pure water.
4.2. Proposed correlation between fatigue strength and delamination wear

Previous published [8,9,23] and unpublished research by the authors implementing multi-fiber copper and copper–beryllium brush sliding electrical contacts in humid carbon dioxide environments revealed that the wear morphology of these systems was predominately flake-like and of a relatively large aspect ratio (typical debris flake length of 100 μm and thickness of 1–10 μm). This type of debris morphology is documented for copper systems in Argibay et al. [8,9] and for silver multi-fiber brushes in Brown et al. [24]. Copper debris gathered from the anode brush from an experiment using copper multi-fiber brushes at high current density (180 A/cm²) similar to the procedure described in [9] was analyzed by L. Singer (Naval Research Lab). X-ray diffraction (XRD) revealed that the flake-like debris was predominately copper, rather than copper oxide (Cu2O), implying that it is not simply a matter of enhanced oxidation resulting in an increased rate of copper oxide growth followed by film delamination on the positive brush (and equally inhibited rate on the negative brush). That is, the wear debris is not simply delamination of copper oxide films. In fact, oxide films are typically on the order of 1–10 nm in thickness [5,21] whereas the copper debris flakes were on the order of 1–10 μm in thickness, indicating that the depth at which delamination was occurring surpassed the depth of oxide by an order of magnitude. Argibay et al. [8] observed an order of magnitude reduction in wear rate at high current density (200 A/cm²) by replacing copper multi-fiber brushes with higher fatigue strength, higher yield strength copper–beryllium multi-fiber brushes, an indirect indication of the importance of fatigue strength.

The authors propose a conceptual model (illustrated in Fig. 5) suggesting a correlation between the modified oxidation rate mechanism proposed by Boyer et al. [5], the fatigue strength of the surface materials, and the wear rate of non-noble metal contacts at high current density in an oxidative medium. In a 1973 publication Nazarenko et al. [25] provided experimental evidence of what they referred to as dislocation trapping under an increasingly thicker oxide film on a zinc single crystal due to cyclic frictional stress. They showed by etching and microscopy that there is an increase in dislocation density under the friction surface with increasingly thicker oxide films. Buckley [26] observed that relatively ductile gold substrates exhibited surface fatigue cracks due to cyclic frictional stress with the addition of a gold halide surface film. Wang et al. [27] reported a 15–30 fold difference in the fatigue strength of copper when experiments were performed in air or high vacuum (with higher fatigue strength in vacuum). Thompson et al. [28] showed that the fatigue life of copper single and polycrystals fatigued in dry nitrogen was approximately 10 fold greater than when in air. They also found that the fatigue strength of copper specimens could not be extended by annealing (in vacuum) alone prior to failure through fatigue, though removal of the top few micrometers (where the majority of damage was observed) through chemical etching elongated the fatigue life of copper specimens effectively indefinitely. Duquette [29] performed fatigue tests on nickel single crystals in vacuum, dry air, and humid air and similarly reported a substantial decrease in fatigue strength while operating in an oxidative environment. Duquette suggests that the source of this phenomenon may be explained in a conceptual model proposed by Shen et al. [30], wherein dislocations nucleating near a metal surface due to cyclic stress are inhibited from propagating and relieving at the surface under increasingly thicker oxide films. With increasingly thicker oxides, Shen also suggests a mechanism of dislocation trapping near the metal–oxide interface. An increase in dislocation density near the surface may correlate to a greater rate of sub-surface crack nucleation and propagation. Shen’s model was based in part on his own experimental data from fatigue testing of aluminum specimens with varying degrees of prescribed surface film thicknesses. The link between dislocation trapping and wear may exist in the theory of delamination wear proposed and developed by Suh et al. [31], Buckley [26] and Duquette [29], and the phenomenon of dislocation trapping under increasingly thicker surface films or oxides as proposed by Shen et al. [30] and Nazarenko et al. [25], which are correlated to the enhanced/inhibited electrochemical oxidation model proposed by Boyer [5].

![Figure 5](image_url)

**Fig. 5.** Illustration of a proposed conceptual model for wear of lightly loaded metal sliding electrical contacts correlating (a) the model of delamination wear (adapted from Suh et al. [31]), (b) a reduction in surface fatigue strength due to surface films or oxides as reported by Buckley [26] and Duquette [29], and (c) the phenomenon of dislocation trapping under increasingly thicker surface films or oxides as proposed by Shen et al. [30] and Nazarenko et al. [25], which are correlated to the enhanced/inhibited electrochemical oxidation model proposed by Boyer [5].
of dislocation trapping. The electrochemically enhanced/inhibited rate of oxide growth is correlated to the asymmetry in wear rate observed in previous published studies of high current density multi-fiber brushes as a function of brush polarity (anode or positive brushes wearing faster). Consequently, for copper sliding electrical contacts operating immersed in an oxidation inhibiting hydrofluoroether liquid medium (where the metal fiber was in an anode configuration, the case of enhanced wear) there should not be and was not observed to be a strong correlation between current density and oxide growth rate (and consequently also wear rate).

5. Conclusions

The experimental research summarized in this paper examines the role of environment on the friction, wear, and contact resistance characteristics of self-mated copper sliding electrical contacts. More specifically, the experiments were designed to remove some of the complexities typically encountered in macroscopic scale experiments with multi-fiber metal brushes. Arguably the most notable conclusion derived from the data is the observed insensitivity of brush wear rate to current density when operating in an oxidation inhibiting fluorinated solvent (a hydrofluoroether). This is in contrast to a previously established correlation between the wear and enhanced electrochemical oxidation of copper (and other non-noble metals) in oxidative environments (humid CO₂, humid N₂, etc.) above a threshold current density [5,9,12,13,37].

The fact that the phenomenon of enhanced wear was not observed in an oxidation inhibiting medium (operating the contact immersed in liquid hydrofluoroether with an He cover gas) supports the theory proposed by Boyer et al. [5] that it is, for a positive (anode) brush, the enhanced rate of oxidation due to the applied electric field across insulating oxide films (needed to drive current flow through the contact) that causes a higher wear rate at high enough current density. The insensitivity of wear rate to current density for these copper sliding electrical contacts at relatively high current density in a non-oxidizing environment suggests that by inhibiting oxidation the film resistance and thus the applied electric field across the film (the voltage drop) must remain low.

The possibility of achieving sustained current densities (400+ A/cm²) while maintaining relatively low, current density insensitive wear rates through a lightly loaded copper sliding electrical contact was demonstrated. This behavior was achieved by immersion of the sliding contact in a liquid medium that inhibited cold-welding of the nominally oxide free copper surfaces, provided effective cooling (mitigating Joule heating), and prevented oxidation. This system exhibited an effective decoupling of current density from metal fiber wear rate for current densities as high as 450 A/cm² based on the nominal fiber cross-sectional area (or greater than 2300 A/cm² based on the measured wear scar area).

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Appendix A. Appendix

This section presents the derivation of an approximate solution for the volume of a toroid segment used in the calculation of worn volume. Fig. 6 illustrates the geometry, variables, and coordinate axes considered. The trigonometry identities of A.1 are used as the starting point (shorthand notation will be used for references to equations; for example, Eq. (A.1) will be referred to simply as [A.1]).

\[
R^2 = (R - \delta_z)^2 + s^2 \quad R - \delta_z = R \cos \theta \quad \text{(A.1)}
\]

A Taylor series expansion of the cosine term is implemented. Only the first two terms are considered, the higher order terms being neglected based on the assumption of small angles.

\[
\cos \theta \approx 1 - \frac{\theta^2}{2} \quad \text{(A.2)}
\]

\[
(A.3) \text{ are derived from (A.1) and (A.2) for the range of values } 0 \leq s \leq a.
\]

\[
\theta \approx \frac{s}{R} \quad \delta_z \approx \frac{s^2}{2R} \quad \text{(A.3)}
\]

\[
(A.4) \text{ correlates the term } h_z \text{ and the parameters } a, s, \text{ and } R.
\]

\[
h_z + \delta_z = d \quad \rightarrow \quad h_z = \frac{a^2 - s^2}{2R} \quad \text{(A.4)}
\]

An expression for the cross-sectional area \( A_z \) corresponding to the worn portion of a slice of the toroid segment is given in (A.5). Note that the small angle approximation is applied once again such that the cross-section is assumed to be circular for all values of \( s \), neglecting the fact that with increasing \( s \) the circular cross-section will become an ellipse.

\[
A_z = \frac{\pi r^2 \phi}{2\pi} = \frac{1}{2} h_z r \cos \phi \quad \text{(A.5)}
\]
Following the same procedure used to derive (A.3), the expressions of (A.8) are found, again assuming small angles for $\varphi$.

$$\varphi \equiv \frac{b}{r}, \quad h_0 \equiv \frac{b^2}{2r}$$  \hspace{1cm} (A.6)

Then the expression (A.5) may be reduced as in (A.7). Note that this approximate expression for the area $A_t$ is not a function of semi-minor radius $b$.

$$A_t \equiv \frac{b^3}{2r} \rightarrow A_t \equiv \frac{h_0}{2} \sqrt{2h_0 r}$$  \hspace{1cm} (A.7)

An expression for the approximate volume of the toroid segment is then calculated by integration of (A.7), with the aid of (A.4), as shown in (A.8).

$$V_{\text{loss}} \equiv 2 \int_{s=0}^{a} A_t ds \rightarrow V_{\text{loss}} \equiv \frac{3\pi a^4}{16} \sqrt{\frac{r}{R}}$$  \hspace{1cm} (A.8)

The small angle approximation results in an under-estimation in volume calculation. This error is evident and quantifiable for the special case of where $r = R$ (a spherical cap). Comparing a closed-form solution for the volume of a spherical cap and the approximate volume calculation (A.8) for the special case of $r = R$, the adjusted expression (A.9) is shown to achieve greater precision.

$$V_{\text{loss}} \equiv \frac{\pi a^4}{4} \sqrt{\frac{r}{R}}$$  \hspace{1cm} (A.9)

The precision of (A.9) increases for increasingly smaller values of the ratio $a/R$, a characteristic of the toroidal geometry (e.g. for $R = 3.0$ mm, $a = 100 \mu$m, $a/R \sim 0.03$). For the example of a spherical cap with $r/R = 1$ and $a/R = 0.1$ the error in expression (A.9) is approximately 0.3%.

### References