MARINE SURVEY SYSTEM FOR DETECTION OF UNEXPLODED ORDNANCE

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ABSTRACT

With support from the Strategic Environmental Research and Development Program (SERDP) and Environment Security Technology and Certification Program (ESTCP), SAIC has developed and demonstrated a marine unexploded ordnance (UXO) survey system designed for use in shallow water (<9.1-m). The air foil shaped sensor platform has two actuator-controlled elevators to control depth, altitude and attitude. The sensor platform incorporates eight cesium vapor magnetometers (0.6-m horizontal spacing) and a time-domain EMI system with a single large transmitter coil and an array of four receiver coils. The sensor platform is towed by a 9.1-m pontoon boat (with a 22-m cable), which houses the data acquisition system and auxiliary electronics required for controlling and monitoring the sensor platform. The sensor platform has two operating modes: altitude mode for maintaining a constant altitude above the bottom; and depth mode for maintaining a constant depth below the water surface. The attitude and depth of the sensor platform is controlled by an autopilot, which feeds output commands to the starboard and port actuators. The autopilot uses attitude information (pitch, roll, and yaw) from a tactical grade inertial measurement unit (IMU) on the sensor platform, velocity data from a real-time-kinematic (RTK) GPS system, heading from the sensor platform magnetic compass, altitude above the bottom from the platform sonar altimeter and water depth from the platform pressure sensor.

The data acquisition system uses a separate GPS unit for synchronization of the system PC clock to GPS UTC time. The system's first demonstration was at the former Duck Naval Target Range. Approximately 121.4 hectares were surveyed in water depths of 1-4 meters using the magnetometer and EMI sensor arrays. An overall target location accuracy of +/- 30 cm was achieved. A total of 100 targets from the 500 target dig list were reacquired and recovered by UXO-qualified divers. Approximately 5% of the recovered targets were UXO items associated with the former range. Additional surveys have been completed in Ostrich Bay (Puget Sound), Lake Erie (Ohio), bays around Vieques and Culebra islands, Puerto Rico) and in the Potomac River (off Blossom Point, MD).

1. INTRODUCTION

As a result of current and past military training and weapons-testing activities, Unexploded Ordnance (UXO) is known to be present at many BRAC and FUDS properties and on many active ranges. Many of these sites associated with military practice and test ranges, contain significant land areas with a marine component. Although it is known that between 4 and 8 million hectares of dry land UXO contamination is associated with Closed, Transferred, and Transferring (CTT) ranges, the fraction of this area that is underwater and inaccessible to standard UXO search technologies is poorly defined; however, it likely exceeds a million acres within the continental US.

In 2003, with funding from the Strategic Environmental Research and Development Program (SERDP) and the Environment Security Technology Certification Program (ESTCP), SAIC designed and developed the marine-towed array (MTA) UXO sensor system [1,2,3]. This system provides a truly unique capability for underwater UXO search systems. The survey products include digitally geo-referenced magnetic anomaly maps of metallic objects buried in the bottom sediments and estimates of the positions, sizes, and burial depths of the individual metallic anomalies.

2. SENSOR PLATFORM DESIGN

The primary component in the design of the MTA was the sensor platform. The requirements for the operation and performance of the MTA are driven by the sensor requirements for UXO detection. This includes both sensor performance and geometry, sensor positioning and survey speed. Both the magnetometers and the EMI sensors must be isolated from metallic interferences and from electrical and electronic interferences associated with the operation of the sensor platform and the tow vessel. Because of this, construction of the sensor platform must be magnetically clean. Two types of UXO sensors were incorporated in this sensor platform; eight Cesium Vapor magnetometers and a Time Domain Electromagnetic Induction (EMI) system consisting of one (1-m X 4-m) transmitter coil (Tx) and four (0.5-m X 1-m) receiver
coils (Rx). These platform characteristics are shown in Figure 1.

![Figure 1: Schematic drawing of the MTA sensor platform](image1.png)

In addition, a series of platform control sensors including; a sonar altimeter, an inertial measurement unit (IMU), a magnetic heading compass, and a depth gauge are required to support the operation of the sensor platform. A wide array of design concepts were considered including; bottom contact (rolling or sliding) sensor platforms, dipping sensor arrays (either mounted on the primary vessel or on a towed floating platform), or a submerged sensor platform (towed by a flexible cable or a rigid rod). After extensive hydrodynamic modeling with Vehicle Control Technologies (VCT), the submerged sensor platform with flexible tow cable was selected. This concept design is shown in Figure 2.

![Figure 2: Perspective view of the MTA sensor platform design [4].](image2.png)

The platform shape is a typical wing outline with a rounded leading edge and a tapered trailing edge; the top and bottom surface contours are flat and parallel. The wing thickness is ~15-cm. The overall width of the sensor platform is 4.7-m, and the length is 4.15-m. Rear body extensions were implemented to provide distance between the detection sensors and the actuators required to control the stern planes.

At the beginning of the project, the preliminary design, including the CAD drawings and materials information, was passed to Structural Composites, Inc. This company is a composite materials design and manufacturing shop. We worked with them to refine the dimensions and shapes from the preliminary design. They were allowed to re-engineer the materials types and specifications based upon the assumption that they would manufacture it. We specified that the final design must rugged enough to sustain grazing encounters with the bottom and 2.5-m/s head-on collisions with rocks or pilings. The skids on the bottom were engineered to make the system bottom heavy; the overall system must be 9.0-13.6-kg buoyant and it must float flat and level. Moreover, the system was required to flood quickly allowing it to submerge easily, and to drain quickly to allow it to be hoisted from the water. Based on these requirements structural composites provided a revised design, which we asked to be submitted to a Finite Element Analysis to evaluate its strengths and weak points, Figure 3. Two cases were considered; the first with a ¼” skin and the second with a 3/8” skin. The maximum deflection in the bottom of the sensor platform was approximately 1-in for the ¼” skin and 0.5-in for the 3/8” skin. For the final design the 3/8” skin was selected because of the additional strength and reduced deflections.

![Figure 3: Deflections on the bottom of the MTA sensor platform with 1/4” fiberglass skin.](image3.png)

Structural Composites used bulk fiber glass composite material to provide strength and weight and closed cell foam to provide floatation and to adjust trim. Following minor modifications, the final structural design was approved and manufacturing began. The sensor platform was manufactured of fiberglass to minimize the overall metallic content. Its dry weight is 590-kg, and it requires an underwater towing force of 160-kg at 2.5-m/s and 5-m depth. When the submerged platform is flooded, the combined weight of the platform and
contained water is ~1360-kg. The final dimensions and weight of the sensor platform were used to make a final pass through the VCT hydrodynamic modeling program. The resulting information was used to establish the platform tow position, the width of the stern planes, determine preliminary settings for the autopilot, determine strength specifications for the tow cable, and engine requirements for the tow vessel. Figure 4 shows an image of the entire structure floating in the water beside the tow vessel.

Figure 4: The assembled sensor platform is shown floating beside the tow boat.

Sensors

The MTA is an integrated Magnetometer/EMI sensor platform. The combined sensor approach of active and passive sensors maximizes detection capability and allows for differentiation between ferrous and other metallic targets. The two sensor types cannot be operated simultaneously because the EMI transmission is received as noise by the magnetometer.

The system contains an array of 8 total field Cesium vapor magnetometers. The cesium vapor magnetometers are mounted with a spacing of 60-cm so that they do not interfere with the mounting of the EM Rx coils. The magnetometers are passive sensors that operate by measuring the earth’s magnetic field. The earth’s magnetic field distorts around ferrous objects (such as pipes, cables, UXO, etc) and the magnetometer sees this distortion as an increase or decrease in the intensity of the magnetic field. The high sensitivity of the cesium magnetometers allow for detection of small targets at relatively large standoff distances [5]. For this system, the magnetometers’ output data is received in two groups of four each at a synchronous rate of 20 Hz. This synchronized aspect is achieved by utilizing one magnetometer as a master and having its output daisy chained with the remaining three magnetometers in its group and then, again with the other four magnetometers in the second group. The magnetometers are individually connected to the Sensor Interface Pressure Vessel (SIPV). This SIPV is depicted as the Sensor Interface Bottle in Figure 5.

The EMI system is an active detector, which uses a time varying electromagnetic field established over a conducting target. In response to this field, a secondary electromagnetic field is produced by a conducting target, which can be detected at a distance, establishing the presence of the target and also, allowing an estimation of its size and shape [6]. For this system the EMI has one large rectangular transmitter coil measuring 4.5-m by 1.0-m and four receiver coils each measuring 1.0-m by 0.5-m. The EMI system has an interconnection box installed between the Tx coil and Rx coil. This interconnection box also contains the preamplifiers, one for each Rx coil. Each preamplifier conditions the raw Rx coil signal and outputs the signal differentially, which allows topside removal of all common mode noise that may have been picked up along the tow cable. Figure 5 shows an image with the hatch covers removed. Several of the sensor components are labeled.

Figure 5: The marine sensor platform is shown with the hatch covers removed.

Platform Control

The platform response is controlled by a closed loop autopilot system. Three primary operational control algorithms were developed for the sensor platform GUI. The first allows the platform to be set at an operator-specified depth below the surface. The second mode is designed to operate the platform at a specified height above the bottom. The third mode is Emergency Rise which brings the platform to the surface if a bottom obstruction is observed that is likely to cause impact with the platform. The control of the platform is accomplished by using the two stern planes, which provide pitch and roll control for the platform. The stern plane positions are controlled by rotary actuators that are coupled via a fiberglass linkage mechanism, Figure 6.
The rotary actuators have three modes of control; position, velocity and torque. Each of the control modes uses the other two as operational limits. Mechanical rotation stops were installed on each actuator to prevent physical damage to the platform during an actuator malfunction. During software initialization, the autopilot routine commands the actuators to rotate in one direction until the unit reaches the mechanical stop. The encoded position is recorded and the actuator is rotated in the opposite direction until it reaches the other mechanical stop. Once the encoder positions for both mechanical stops are known, the software calculates virtual stops which are just inside the mechanical stops. These virtual stops provide the operational limits of the rotary actuators during operation.

The autopilot control system uses inputs from the IMU, the pressure transducer, magnetic compass, sonar altimeter, and the tow vessel GPS velocity to control the platform operation. The autopilot is designed to provide depth/altitude control and roll control to the platform. Roll control is given top priority in the program to keep the sensor platform level. The roll is measured using output from the IMU. Depth/altitude control is maintained using outputs from the pressure transducer for depth, and the sonar altimeter for altitude.

Figure 7 shows the use of the autopilot to control the sensor platform depth and attitude. The platform was operating in depth (below surface) mode for this test. These results were from a shakedown test on Lake Jordan in central North Carolina. In the center panel the tow vessel speed is plotted. During the first 110 seconds of the plot the boat was set to a speed of 1.8-m/s; at 410 seconds the speed was increased to 2.2-m/s. Wind gusts or changes in depth commands affect the speed by small amounts. The upper panel shows the platform depth below the surface. The command depths are shown in red. The sensor platform response to these commands is shown in blue. In general, it takes about 10 seconds for the platform to respond to a commanded depth change of 1-m. To depths of 5-m, the commanded depth is maintained to better than 10-cm. The platform will not quite maintain the commanded 6-m depth (with the 16-m tow cable length). The design goal for these conditions was a maximum depth of 5-m. Lengthening the cable or moving the tow point further aft would allow deeper operation. In the bottom panel the elevator pitch angle is shown in green. The commanded depth changes invoke an 8° elevator command. The autopilot returns this to the smaller value required to maintain depth when the command depth is reached. The actuators that control the elevators have a ±15° freedom of movement. 7° of the 15° are reserved for priority control of the platform roll. In general, the platform roll is maintained to within 0.5° of level. The autopilot shows a similar level of control over the sensor platform when operating in height above the bottom mode.

3. PLATFORM POSITIONING

Each individual sensor position is derived by translating the antenna position of a top-side mounted real time kinematic (RTK) GPS system to each sensor location. The horizontal positional accuracy at the towpoint is ± 1-cm while communicating with the stationary land based RTK system. To translate this position to the sensor positions, we require knowledge of the tow vessel attitude, the effective tow cable length and angles relative to the tow vessel, and the sensor platform attitude, Figure 8.

To determine the precise location of each sensor we must translate the absolute GPS antenna position (latitude, longitude, and height above ellipsoid [HAE]) to each sensor location. The largest component of the error budget in deriving the sensor positions in the horizontal plane is the angle of the tow cable with respect to the heading of the boat. This angle is derived from the two GPS antennas at the bow and stern of the boat and the digital encoder, which measures the angle of the tow cable to ±0.01°. Summing all the components of the horizontal error budget we predict an uncertainty of about ±15 cm in the sensor positions. This is consistent with measurements made against calibration targets. High precision depth sounders at the bow
of the boat and on the sensor platform, along with a digital depth gauge on the sensor platform, allow the height above ellipsoid for the sensors and the absolute height of the sensors above the bottom to be determined. In addition there are digital magnetic compasses on the boat and in the pressure bottle on the sensor platform.

4. TOW VESSEL

The tow vessel is a 30 ft triple pontoon boat, Figure 9. It is about 8 ft wide and is powered by a 140 hp outboard engine. The railings and most components from the forward half of the deck were removed. A hoist was mounted forward to lift and remove components (racks, generators and the sensor platform). Tie downs, cleats, and an additional captain’s chair were mounted aft. The tow assembly, developed specifically for this application was mounted at the rear. These assemblies provide storage for excess cable and an electrical interface bulkhead for the wet-re mateable cable connectors. GPS antennas are mounted on 8 ft masts at the center points of the bow and stern. The master antenna is located immediately above the tow point.

The sensor platform is towed by a 16-m or 22-m cable attached to a custom tow point located at the center of the boat at the stern. The tow cable has all of the electrical wiring embedded in addition to an integral Kevlar strain member, which has a working load rating of 453-kg and a breaking strength of 2495-kg. The 22-m cable length allows routine submerged operation up to 7-m depths while the 16-m cable length is used for 5 m maximum depths. At reduced speeds we can operate and control the system at water depths > 9-m with the 22-m tow cable. In case of severe impact of the platform with the bottom structure, we have designed and installed a breakaway link in the tow cable attachment, which parts at 612-kg. This is approximately four times our operating force and one-quarter of our tow cable breaking strength. Additionally, we have installed a SEAFLEX® rubber hawser between the tow point and the weak link. This hawser damps out much of the force in the tow cable related to sea conditions, and reduces down time during surveying because of breaking the weak link.

The electrical connectors from the tow cable to the bulkhead connector at the rear of the boat are designed to part at 22.6-kg. These connectors are wet re-mateable. This feature prevents any permanent damage if the sensor platform is snagged and causes the stainless steel weak link to break. If the weak link breaks and the vessel operator is not able to stop the vessel fast enough, the tow cable becomes disconnected with a rope and a buoy attached to it for easy recovery.

The tow point is a specially designed fixture, which allows us to measure the tow cable azimuth angle with respect to our tow vessel heading. It is comprised of a free-wheeling arm coupled to an optical encoder via two spur gears of equal diameter. The arm is mounted on the shaft via two needle bearings with top and bottom thrust bearings yielding a very low resistance of movement, Figure 10. The optical encoder has a resolution of 0.1° and is read via a dedicated CPU, which is used for position, calibration and output data formatting. Its output data are serially transmitted to the data acquisition system (DAQ) at 10Hz. The tow point fixture also serves as the primary GPS antenna mounting location and provides the mounting for the two-way RF radio communications link to the RTK GPS base station.
5. SURVEY OPERATION AND DATA PRODUCTS

The survey is conducted with the information provided by the pilot guidance display unit mounted at the driver’s console, Figure 11.

The survey grid or transects are programmed into the accompanying computer. The high brightness display can be scaled to guide the driver to the survey area and the beginning transect. The view scrolls as the vessel moves and allows the driver to maintain the course during the survey. The screen also displays the distance off track, the transect number, the GPS fix quality and other information. The water depth, measured by a high precision depth sounder at the bow, is also displayed to the driver. Under normal survey conditions, an abrupt depth change is displayed to the driver 5-8 seconds before the sensor platform arrives at the same position. All data streams from sensors on the sensor platform and from sensors on the tow vessel are monitored and recorded on one of the three computers in the data acquisition racks, The data are time stamped using GPS Universal Coordinated Time (UTC).

All survey data are typically preprocessed overnight to assure completeness and for quality control purposes. During preprocessing, data are checked for registration, filtered and/or smoothed and vessel turn-arounds are typically edited from the data. The output of this process is a mapped data file suitable for target analysis. If time requirements mandate, one person on site can typically handle the data preprocessing and reduction to an updated mapped data file for next-day analysis. Figure 12 shows a typical block of data from a mapped data image from the survey at Ostrich Bay in Bremerton, WA.

Final work products from the survey include magnetic anomaly maps, target reports containing locations and calculated parameters of size, inclination, azimuth and depth of the analyzed anomalies.

We have carried out extended demonstration surveys on the Currituck Sound [8] (smooth sand and mud bottom), the protected waters of Ostrich Bay [9] (boulders, broken off pilings, steep walled dredge cuts, 3.6-m tides and water up to 12-m deep), Lake Erie [10] (sand bottom with bedrock outcroppings, cables and pipelines, 1.2-m waves and requirements to operate 19-km off shore), Vieques [11] and Culebra [12], Puerto Rico (open ocean environment, fringing coral reefs, shipwrecks in the survey area, and water up to 12-m deep), and the Potomac River at Blossom Point, Maryland.

Figure 10: Tow point showing angular encoder and the break away weak link.

Figure 11: Pilot guidance display showing survey grid.

Figure 12: Magnetic anomaly image (mapped data file) from a ~5 ha area near the center of Ostrich Bay.
were able to carry out successful and productive UXO surveys in each of these areas. Recoveries of more than 1,000 targets at these sites has benchmarked the location accuracies and size and burial depth predictions of the data analysis system. Except for occasional breakdowns and weather delays, survey productivity has averaged about 14 hectares/day. The entire system is shown during survey in Figure 13.

Control of the platform by the autopilot has been close to perfect. Typically roll is controlled to $<0.5^\circ$ and depth is controlled to $\pm 10$-cm. These conditions apply up to the maximum deployable depth. Under Sea State 1 conditions, even with cross winds, the driver can typically maintain the survey course within a few tenths of a meter of the survey grid line.

To our knowledge, the MTA is the only currently available shallow water UXO survey platform that can tackle wide area surveys with substantial productivity. To its advantage, it has the capability to conduct either transect or comprehensive surveys at production rates in excess of 2.4 hectares/hour while maintaining the sensors at a reasonably fixed distance above the bottom. The demonstrations that we have completed effectively define the capabilities and limitations of the MTA system.

6. CONCLUSIONS

The MTA system offers the first efficient and automated modern UXO survey capability that can provide fully geo-referenced survey products to support shallow water UXO clearance operations. As it is constructed, the Marine Towed Array is a very complex R&D system. It is likely too electronically complex, too heavy, and too expensive to be a competitive commercial instrument as it is currently configured. However, we have learned enough from its design, performance, and operation to design a field-worthy prototype that would likely weigh 60-70% less, and be self-contained and transportable on a single boat trailer.

Mechanically, we currently recognize two shortcomings of the system. It requires the use of an improved boat launch ramp to deploy and recover and is very difficult to maneuver in very shallow water and in the narrow access in many marinas. In many marine areas this is a problem. The system was originally intended to be launched and recovered at sea using the hoist mounted on the vessel surface. This proved to be unsafe, requiring the survey platform to be launched and recovered from an improved launch ramp. Because of this, the platform must be ferried under tow morning and night from the dock to the survey point and back. In rough seas, the maximum ferry speed is $\sim 1$-m/s. A lighter weight system which could be recovered would allow the system to be ferried on the surface of the pontoon boat at speeds greater than 10-m/s. This would greatly improve survey coverage rates on large sites that require long ferries from the dock to the survey area.

The other major improvement recommended for the system is a smaller diameter and longer tow cable. This would allow for survey greater survey speeds in deeper water $>9$-m and survey in deeper water. A hydrodynamic modeling study will be required to determine the maximum survey constraints of the improved system.

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IMPROVING THE USABILITY OF LIQUID MOTOR FUELS: 
THE ACTIVE VAPOR UTILIZATION SYSTEM

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ABSTRACT
The Active Vapor Recovery System (AVUS) was developed to simultaneously reduce both evaporative and tailpipe emissions from motor vehicles. The AVUS produces liquid fuel from tank vapor generated in normal vehicle operation, for exclusive use during the starting and warm-up periods. The system can process hydrocarbons vapors during refueling, hot soaks, diurnal temperature swings and during normal drives (running losses), considerably reducing evaporative hydrocarbon emissions. The starting fuel produced by AVUS is highly volatile, with a vaporization rate nearly twice that of isopentane, one of the most volatile constituents of pump gasoline. By comparison, the initial boiling point temperature of its parent gasoline corresponds to the temperature at which up to 70% of AVUS condensate will vaporize. The use of AVUS condensate during staring and the subsequent warm-up period will facilitate enhanced combustion and greatly reduced tailpipe emissions during cold starts. The AVUS system can significantly reduce the two most dominant forms of vehicular hydrocarbon emissions simultaneously. No emissions test were conducted for this work, but it is expected that AVUS condensate should facilitate tailpipe hydrocarbon emissions reductions of at least 80%.

INTRODUCTION.
Hydrocarbon (HC) emissions are a persistent threat to the environment. Commonly emitted HC species include precursors to smog formation and agents that are acutely toxic to human, animal and plant life. Moreover, the U.S. Environmental Protection Agency (EPA) reported that automobile sources contributed 44% of the national emissions inventory of volatile organic compounds (VOC) in 2002 [1]. Most VOC emissions released from modern vehicles are generated through incomplete combustion of fuel (tailpipe emissions) and by lost containment of vapors released from fuel on board (evaporative emissions). A few facts bear recognition:
(a) The cold-start period, which includes the first 60-90 seconds of engine operation after starting, is responsible for 60-95% of all tailpipe hydrocarbon emissions during emissions certification testing [2, 3].
(b) Evaporative HC emissions have been conservatively estimated at 3-4x higher than tailpipe emissions during routine driving [4].
(c) Starting fuel generated by the On Board Distillation System—an on-board fuel preprocessor that collected the volatile fractions of gasoline for exclusive use during starting—was demonstrated to reduce overall tailpipe hydrocarbon emissions by more than 80% [2]. One of the conclusions from that study and separate research by Stanglmaier et al [5] was that an ideal starting fuel would be rich in HC species no heavier than C6.
(d) As one would expect, the vapor space in a vehicle fuel tank predominantly consists of the lightest fractions of gasoline. Speciation of the vapor above liquid gasoline at 21 °C revealed that the vapor was dominated by three highly volatile species: isobutane (2-methylpropane), n-butane and isopentane (2-methylbutane) collectively accounted for 78% of the vapor composition [6].
(e) Older vehicles have disproportionately higher evaporative and tailpipe hydrocarbon emissions [7-9].

It follows naturally from the preceding statements that significant reductions of hydrocarbon emissions are possible if both evaporative and cold-start tailpipe emissions can be eliminated. It is also apparent that fuel tank vapor makes an excellent starting fuel. Moreover, hydrocarbon emissions from cold starts and fuel evaporation could be reduced or eliminated simultaneously if fuel tank vapors were recovered for use as a starting fuel.

The idea of starting a vehicle with fuel tank vapors is not new; this approach has been attempted many times previously, though with mixed success. Many attempts have been made to develop passive cold starting systems that could induct hydrocarbon vapors from the carbon canister. For the most part these devises tended to suffer from the same fundamental imperfections: (1) there was no way to know conclusively how much vapor could be drawn from the canister; (2) the vapors inducted from the
carbon canister were mixed with air, and the resultant mixture was of unknown strength; and (3) distributing the vapors evenly among cylinders is very challenging. Predictable and robust fueling is vital for cold-starting, more so than any other operating regime. Thus, most carbon canister vapor induction systems were unsuitable. A promising exception is the Vapor Cold Start System (VCSS) presented by Servati et al [10], which is notable in that it used an active sensor to determine the hydrocarbon concentration in the vapor canister.

A Novel Solution

The focus of this research is a new concept for an active on-board vapor recovery system – the Active Vapor Utilization System (AVUS) – that collects fuel tank vapors for use as a cold-starting fuel. The AVUS is installed in a conventional fuel system between the fuel tank and the carbon canister. The operation is as follows: air and vapors that would normally be routed to the carbon canister are compressed and cooled below the dew points of the most-volatile fuel-borne hydrocarbons (usually n-butane). The condensate is collected for pressurized storage; the remaining noncondensables (mostly air, with trace VOCs) are routed to the carbon canister for normal treatment. A schematic of the system is shown in Figure 1. AVUS operate via the principle of “natural” distillation. That is, it does nothing to actively generate fuel vapors; rather, AVUS collects vapors normally generated which are typically consumed by the engine.

There are many significant features of the AVUS system. First is the unique active compression/cooling that promotes the removal of air from the collected vapor. This feature eliminates the largest obstacle to the use of tank vapor for engine starting by facilitating fuel/air mixtures of known strength during starting. The second significant feature is retrofit capability. Existing proposed solutions to the cold start HC problem tend to be very difficult to implement in retrofit scenarios [2]. AVUS can be easily retrofit to existing vehicles, even those that are carbureted. Finally, exclusive to AVUS is the ability to operate without the engine running – during refueling or hot soak, or while sitting in the driveway – in addition to operation with the engine running.

A pertinent concern is the question of whether enough tank vapor would be available when ambient temperatures are low. There would be little thermal driving force for fuel evaporation at the same time that the need for a volatile starting fuel is greatest. The low thermal driving force for vaporization, however, is balanced by increased volatility of fuels supplied for wintertime use. In cooler climes, motor gasolines are blended with higher fractions of volatile species (e.g. n-butane, isobutane) to enhance cold-start performance. Consider Figure 2, which shows the variation in fuel driveability index for pump gasoline commercially available in the U.S. in 1998 [11]. Driveability Index ($DI$) is a common measure of fuel volatility, where $DI = 1.5T_{10} + 3T_{50} + T_{90}$. $T_{nn}$ represents the temperature at which $nn\%$ of a multicomponent fuel vaporizes when heated per the ASTM D86 protocol. Lower $DI$ values correspond to higher volatility. Given the volatility increase in the fuel available to consumers during wintertime, the AVUS concept should be viable for cold weather operation.

![Figure 1. Schematic of the Active Vapor Utilization System, depicting installation within a conventional evaporative emissions system. AVUS components are denoted by heavy red lines and bold type.](Image)

![Figure 2. Variation in driveability index in 1998 survey of U.S. pump gasoline [11].](Image)

**EXPERIMENTAL SETUP.**

The primary goal of the research presented here is determination of the feasibility of the AVUS concept with respect to generating a suitable cold-starting fuel from normally occurring tank vapors. Thus, a laboratory-scale version of the AVUS was constructed as depicted in the schematic in Figure 3.

A 15 liter aluminum fuel cell represented the main fuel tank; the fuel cell was heated by two hot plates situated underneath a ~20 mm thick aluminum heat spreader. A pump circulated fuel within the tank to further help prevent local hot spots. The test fuel was heated to moderate temperatures typical of those induced by diurnal, hot-soak and running-loss conditions. Diurnal
losses are induced by the daily sun cycle. Hot-soak losses result from heat buildup immediately after vehicle shutdown. Running losses occur during normal vehicle operation; vaporization is driven by radiation from hot pavement, the hot exhaust system in close proximity to fuel tank, etc. Refueling losses are caused by vapor displacement during fuel replenishment. Note that proper simulation of refueling losses would require a full-size fueling system, and was outside the scope of this proof-of-concept project. A nitrogen purge was used to displace air within the test tank before each test. The test fuel was summertime blend pump gasoline (87 octane rating), purchased from a nearby fuel station. Each test began with 6 L of fresh parent gasoline, all sourced from a single batch that was stored sealed in controlled environment to preclude testing complications that would arise from normal day-to-day variability in the local fuel supply.

![Figure 3. Schematic of the laboratory-scale AVUS, with pressure transducer (P) and thermocouple (T) mounting locations identified.](image)

The (dual-stage, diaphragm-actuated 800 kPa capacity) compressor was activated when the fuel tank pressure reached a preset value (110 kPa absolute), simulating the action of a conventional fuel system, where the combustion of canister vapors is typically commanded when fuel tank pressure exceeds a certain amount. Employed downstream of the compressor was a 200 W thermoelectric cooler acting as a condenser. Finally, at the condenser exit was a simple single-stage vapor/liquid separator. A solenoid valve atop the separator was periodically activated when pressure exceeded 700 kPa absolute to release noncondensables.

**Test Protocol**

The test series was designed to simulate fuel system running losses at main fuel tank operating temperatures of approximately 20, 30 and 45 °C. These temperatures were chosen to reasonably reflect summertime fuel tank conditions. **Figure 4** shows fuel tank temperature data from three vehicles, as reported by Tanaka *et al* [12]. Each of these vehicles was driven over the LA-4 drive cycle (the first two-thirds of the cycle used to simulate urban driving in Federal emissions testing), which is also shown in the figure. In **Figure 5** are fuel tank temperature data reported by El-Sharkawy and Jeffers [13]. The test vehicle for this testing was driven through a drive cycle as follows: one LA-4, two minutes idle, two New York City cycles, two minutes idle, one LA-4. In both published studies the test vehicles were driven on chassis dynamometers during SHED (Sealed Housing for Evaporative Determination) testing for running loss evaporative emissions. These published temperature data show that the 20-50 °C fuel tank temperatures in our testing is a reasonable representation of a range of real-world fuel tank temperature conditions.

![Figure 4. Fuel temperature profiles from three test vehicles as reported by Tanaka *et al* over the (displayed) LA-4 drive cycle [12].](image)

![Figure 5. Fuel temperature data reported by El-Sharkawy and Jeffers during SHED testing [13].](image)
The fuel tank temperature was held constant for the tests, each lasting until at least 500 mL of condensate was generated, or until an hour elapsed. Samples were taken of the parent fuel, the condensate generated and the residual fuel remaining in the main fuel tank. Prior to sample collection, the condensate was chilled to a temperature below 4 °C to minimize vapor loss (AVUS condensate typically boils at room temperature). The parent and residual fuels were sampled at ambient temperature and pressure. Distillation profile testing was performed per ASTM D86 using a Koehler Instruments K45000 Front View Distillation Apparatus. The condensate was Group 0; parent and residual fuels were Group 1. Throughout the duration of AVUS operation and D86 testing, the laboratory ambient temperature was 21 – 22 °C.

Additionally, a simple yet effective volatility test was devised to complement the distillation profile testing. This test – dubbed sTGA – is a simplified version of conventional thermogravimetric analysis. Fuel samples were placed in 4 mL glass cylindrical vials. The sample vials were filled to just below the neck of the bottle, exposing the largest area of liquid possible to air. Because the cross-sectional area of the flask is constant below the neck, the surface area of liquid exposed was constant for the duration of the test. Each the samples was opened and placed on a Sartorius A211P microbalance with the sliding glass doors left open for 40 minutes in a 20 ºC quiescent atmosphere. The room temperature and sample mass were recorded at two-minute intervals.

RESULTS AND DISCUSSION.

Distillation Profile

Figure 6 shows two sets of distillation curves for AVUS fuel samples taken from running loss simulations conducted at main tank temperatures of 20, 30 and 45 °C. On each graph are parent fuels from all tests and the corresponding condensates (a) and residual fuels (b). Boiling points of species commonly found in gasoline are displayed as well.

The AVUS condensates are markedly more volatile than the parent fuels from which they were derived. For example, the initial boiling points (IBP) of the parent fuels corresponds temperature to T_{50} – T_{60} of the condensates; parents’ T_{10} corresponds to condensates’ T_{75} – T_{80}. The low temperature extension of the condensate distillation curve relative to the parent fuel shows pronounced vaporization advantages over the cold-start and warm-up regimes. The condensate should be capable of much-improved mixture preparation, reducing the need for startup overfueling, reducing the associated emissions and improving cold driveability.

Boiling point examination indicates that the AVUS condensate has a high concentration of volatile C_4 and C_5 species. This is especially notable considering the parent fuel — a summer blend crafted for 35 – 40 °C local ambient temperatures — contains relatively low concentrations of these species.
The AVUS residuals are generally not much less volatile than their respective parent fuels, suggesting that the mass of volatile ends lost by the main fuel tank is not significant. This could also imply that the condensate generation rate is low, though generation quantification was not within the scope of this study.

Both the condensates and residual fuels display a noticeable level of temperature dependence. Lower main fuel tank temperature shifts vaporization toward the lighter species and reduces vaporization rates. The opposite is true for higher tank temperatures. The net result is that higher main tank temperatures generate heavier condensates and deplete more light-ends from the main tank, leading to heavier residual fuels.

A complete summary of the distillation data is presented in Table 1.

<table>
<thead>
<tr>
<th>Sample</th>
<th>$T_{10}$</th>
<th>$T_{50}$</th>
<th>$T_{90}$</th>
<th>DI [°C]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Parent</td>
<td>50</td>
<td>75</td>
<td>158</td>
<td>457</td>
</tr>
<tr>
<td>20 condensate</td>
<td>28</td>
<td>38</td>
<td>58</td>
<td>215</td>
</tr>
<tr>
<td>30 condensate</td>
<td>27</td>
<td>39</td>
<td>64</td>
<td>223</td>
</tr>
<tr>
<td>45 condensate</td>
<td>34</td>
<td>46</td>
<td>73</td>
<td>262</td>
</tr>
<tr>
<td>20 residual</td>
<td>52</td>
<td>86</td>
<td>157</td>
<td>493</td>
</tr>
<tr>
<td>30 residual</td>
<td>53</td>
<td>92</td>
<td>158</td>
<td>513</td>
</tr>
<tr>
<td>45 residual</td>
<td>60</td>
<td>109</td>
<td>164</td>
<td>581</td>
</tr>
</tbody>
</table>

**sTGA Analyses**

Vaporization rates of the AVUS condensate, the fuel from which it was produced and the residual fuel left behind in the main fuel tank were compared to those of three reference neat fuels: isopentane (2-methylpentane), toluene and isooctane (2,2,4-trimethylpentane). These references fuels were chosen because (1) they make up a large fraction of the constituents of pump gasoline, and (2) there is a wide range in the boiling parameters among them, facilitating good characterization of the test fuels. The vaporization rates of the test and reference fuels are shown in Figure 7, obtained by monitoring the mass of samples exposed to the atmosphere. All masses shown are normalized by the starting mass of each sample. Of the reference fuels, the vaporization rates of toluene (boiling point 110.6 °C) and isooctane (BP 99.3 °C) are virtually indistinguishable, with isopentane (BP 28 °C) vaporizing at much higher rates. This was expected, given the similar boiling points of the two heavier species, which are both much higher than that of isopentane.

The AVUS condensate exhibited a vaporization rate nearly twice that of isopentane. Since isopentane is one of the most volatile species present in gasoline, this is a very positive outcome. The original pump gasolines and residuals exhibit similar vaporization rates, though the residuals appear to have an overall lower vaporization rate (many more tests would be required to discern the difference with confidence). This would be expected, since light ends have been removed. The similarity between the pump fuels and residuals suggests that the AVUS process is not dramatically altering the fuel remaining in the tank. This is indicative that the fuel vapors processed by AVUS are those that would have ordinarily been lost from the tank; i.e., AVUS itself is not
driving the vaporization process. The ramification is that AVUS is not likely to reduce light ends in the parent fuel so much that a vehicle could not start on main tank fuel, a concern when fuel is actively heated (or placed under reduced pressure) to drive distillation [2]. The average vaporization rates are summarized in Table 2.

### Table 2. Average vaporization rates of reference fuels and AVUS condensate.

<table>
<thead>
<tr>
<th>Test Fuel</th>
<th>Rate [mg/min]</th>
</tr>
</thead>
<tbody>
<tr>
<td>isopentane</td>
<td>10.7</td>
</tr>
<tr>
<td>toluene</td>
<td>0.3</td>
</tr>
<tr>
<td>isooctane</td>
<td>0.4</td>
</tr>
<tr>
<td>Pump gasoline</td>
<td>4.1</td>
</tr>
<tr>
<td>AVUS condensate</td>
<td>18.0</td>
</tr>
<tr>
<td>AVUS residual</td>
<td>3.7</td>
</tr>
</tbody>
</table>

**Energy Consumption, Operating Considerations**

Fuel flow rates through the AVUS were not measured; thus, energy consumption rates are based upon electrical demand of the compressor and condenser. The fuel pump and hot plate were not included because they would not be added to a vehicle in which the AVUS were installed. In future work, the fluid flow rates will be recorded.

In normal operation, the vapor separator/collection tank was vented at 6.5 bar to release noncondensables. Venting was stopped when the separator pressure fell to 5.5 bar. Thus, 6.5 bar was the compressor’s highest output pressure (nominally, accounting for pressure drop between the compressor and the separator). At this pressure, the compressor’s power demand was 240 W (~20 A at 12 VDC). Note that the pressure that the compressor must overcome is due only to the vapor pressure of the AVUS condensate in the storage tank. However, considering the vapor pressures of isobutane, butane and isopentane at moderately elevated temperatures — 5.3, 3.9, 1.7 bar at 40 °C, respectively — the compressor pressure from these tests is not unreasonably high.

The condenser power demand was 360 W (15 A at 24 VDC). During normal operation, however, the condenser fins frequently iced over, and it would cool the fluid inside to sub-ambient temperatures (5 – 10 °C collection tank temperatures were very common). For this application, the condenser was somewhat oversized.

Although this research was strictly a laboratory-scale experiment, on-vehicle power demand can be estimated by making several assumptions. First, we assume that the worst-case scenario is refueling, where vapor is being displaced at the main fuel tank refill rate, which is currently limited by the EPA to 37.85 liters/minute. Assumed compressor operating conditions are as follows: inlet temperature = 50 °C, inlet pressure = 1 atm, outlet pressure = 6.5 bar. The fluid is assumed to be half air, half butane (by mass), and the compressor is assumed to operate at 30% isentropic efficiency. The condenser is assumed to operate at 20% efficiency and cool the fluid to 20 °C. With these conservative assumptions based on this unoptimized benchtop version, the AVUS worst-case power consumption is estimated 2.5 kW, well below the 7+ kW typically demanded by automotive accessories such as power steering and air conditioning [16]. In practice, however, we expect post-optimization power consumption well below 1 kW.

**CONCLUSIONS.**

The AVUS system can produce — from normally occurring fuel tank vapors — a highly volatile fuel that should improve combustion stability and reduce emissions during the starting and warm-up periods. Up to 70% of the AVUS condensate can vaporize at the initial boiling temperature of its parent gasoline, and it exhibits a vaporization rate nearly twice that of isopentane, an excellent starting fuel itself. Furthermore, because it relies on tank vapors for operation, AVUS can also reduce evaporative emissions. The AVUS condensate is expected to outperform the distillate produced by the On-Board Distillation System, thus facilitating tailpipe HC emissions reduction in excess of 80% (over the FTP drive cycle). The next step in this research is to quantify these assertions. Thus, a new version of AVUS will be designed and installed on-vehicle for proper SHED testing for evaporative emissions evaluation over a complete battery of diurnal, refueling, hot soak and running loss tests. Moreover, conventional FTP drive cycle testing with condensate stating fuel is warranted to measure AVUS benefits regarding tailpipe emissions.

**ACKNOWLEDGMENTS.**

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IMPROVING THE USABILITY OF LIQUID MOTOR FUELS: PREDICTING FUEL VOLATILITY VIA ARTIFICIAL INTELLIGENCE
PART I – NUMERICAL SIMULATION

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ABSTRACT
A relatively simple phase equilibrium algorithm has been demonstrated to have the capability to predict a liquid fuel composition for a surrogate (synthetic) fuel, given the corresponding hydrocarbon vapor concentration. Ideal gas and liquid behavior was assumed to reduce computational overhead. The result was that the model was most accurate (< 1% error compared to NIST SUPERTRAPP) when predicting compositions of closely related HC species. Interchangeability among surrogate species predicted by the model was experimentally verified with respect to vaporization, proving that boiling point range is the property of most importance when selecting surrogate fuel species. Experimental studies are currently underway to verify the model’s accuracy in real and simulated engine fuel/air preparations systems. Future work will include incorporation of real-fluid behavior into the model to improve prediction accuracy for dissimilar hydrocarbons and in operating conditions near the single-phase regions for given mixtures.

INTRODUCTION
Automobiles are responsible for a significant portion of hydrocarbons (HC) released to the environment. In 2002, the US Environmental Protection Agency estimated that mobile sources were responsible for 44% of the national inventory of volatile organic compounds [1]. The majority of hydrocarbon combustion emissions from on-road vehicles occur during transient operation, particularly cold-starts (engine at ambient temperature), during which temperature and pressure within the engine are rapidly changing. Measured over the industry standard FTP protocol, 60 – 95% of all tailpipe HC emissions occur during the first 60 – 90 seconds after start [2–4]. One of the root causes is gasoline’s relatively low volatility and associated poor fuel vaporization. At moderate temperatures, only 10 – 30% of the injected fuel vaporizes and joins the combustible mix [3], necessitating generous over-fueling to achieve robust starting. Moreover, the volatility of commercially available pump gasoline varies considerably both geographically and seasonally. Figure 1 shows distillation curves representing typical summer ($T_{10} = 54 ^\circ C; T_{50} = 99 ^\circ C; T_{90} = 171 ^\circ C$) and winter ($T_{10} = 41 ^\circ C; T_{50} = 88 ^\circ C; T_{90} = 166 ^\circ C$) blends, along with a “hesitation” fuel ($T_{10} = 63 ^\circ C; T_{50} = 112 ^\circ C; T_{90} = 181 ^\circ C$). A fuel’s distillation curve shows how much fuel vaporizes at a particular temperature when tested per the ASTM D86 distillation protocol. For instance, looking at the summer blend in Figure 1, $T_{10} = 54 ^\circ C$ represents the temperature at which 10% of the sample had vaporized. Hesitation fuel is the lowest volatility fuel typically expected to be found in the fuel supply, so named because an engine control scheme not properly calibrated for it tends to cause noticeable hesitation in response to throttle opening.

![Distillation Curves](image)

Figure 1. Distillation curves of average 2003 U.S. commercially available summer and winter gasoline blends and a certification “hesitation” fuel [2, 5, 23].

At a given temperature, the amount of fuel vaporization is a strong function of fuel type. At starting temperatures, overfueling is needed in order to generate a combustible mix. The engine computer has no knowledge of the fuel on board a priori, and is typically programmed to assume the worst case: hesitation fuel. Thus, fueling
during a cold start can be 10 – 20 times the stoichiometric amount [2-4].

The problem of fueling inaccuracy is not limited to the cold start transient. Even in an engine at operating temperature, some of the injected fuel will join intake port and/or in-cylinder fuel films. The mass and mass rate of change of these fuel films is a function of temperature, pressure and fuel composition. During throttle-opening transients (increasing intake pressure), the film mass grows, creating a lean excursion unless additional fuel is injected as compensation. Conversely, during throttle-closing transients, the decreasing fuel film mass will cause rich excursions. These air-fuel excursions increase emissions and fuel consumption while decreasing driveability. Modern engine controllers have transient fuel compensation capability, but it is clear that a control scheme that employs knowledge of the fuel distillation profile would be beneficial in reducing transient fueling complications in all transient operating conditions.

This research is part of a larger program with the goal of improving emissions, fuel consumption and driveability by increasing the capability of an engine controller to understand liquid/vapor fuel dynamics. Specifically, the research discussed here aims to develop engine control schemes that employ a more complete “understanding” of fuel volatility. An engine computer armed with compositional information and the means to perform volatility calculations should be capable of significantly improved fueling accuracy. However, motor fuels are specified by distillation profile rather than composition, and they typically contain hundreds of constituents. Modern engine controllers do not possess sufficient computing power to calculate volatility from that many components, certainly not in real time. A simplified approach is necessary.

Problem Statement

Several techniques for estimating fuel volatility at engine operating conditions have been developed. Most of these models employ surrogate fuels with a limited (<20) number of components to represent gasoline [3, 5, 6]. These models tend to be empirical in nature, requiring unique calibrations for each engine to which the model is applied.

The paramount goal of this work is to develop an algorithm that can calculate fuel volatility via the principles of phase equilibrium. More specifically, if the HC vapor concentration (in a volume of air) is known, the algorithm is tasked with accurately predicting the composition of the pre-equilibrium liquid fuel from which the vapor would have emanated. In practice, the most desirable method for determining HC vapor concentration will be via interpretation of post-combustion oxygen sensor readings. Alternatively, an inexpensive in-tank HC sensor could be employed. Immediate plans are to use fast-FID sensing to collect experimental data for algorithm validation. The problem statement is illustrated schematically in Figure 2. A chief requirement of this algorithm is low computing overhead; real-time operation (~ engine cycle frequencies) is highly desired.

![Figure 2. Depiction of the problem statement](image)

This is the first in a series of manuscripts detailing the research and development of the control algorithm and its accompanying engine controller. This paper describes initial proof of concept testing of the routine and selection of the potential surrogate fuel ingredients. The basic algorithm is comprised of two modules, a cubic equation of state for predicting phase equilibrium and artificial intelligence to determine fuel composition. The 1978 version of the Peng-Robinson Equation of State (EOS) (hereafter referred to as PR78) is employed for phase equilibrium calculations [7, 8]; particle swarm optimization is the foundation of the AI engine.

Particle Swarm Optimization

Particle Swarm Optimization (PSO) is a stochastic artificial intelligence problem solving technique. PSO is a type of swarm intelligence, which generally describes problem-solving techniques displayed by social animals. Essentially, group members (particles) react to the actions of their fellow members as the population collectively solves a problem. Particularly, PSO is inspired by animal social behaviors such as bird flocks, fish schools or swarms of flies. Kennedy et al describe PSO succinctly as “evaluate, compare, imitate” [9].

Many aspects of PSO are similar to more popular AI techniques, such as genetic algorithms (GA). Populations of particles are seeded; each particle within the population is repeatedly evaluated for fitness; and successive generations yield improved results. However, in PSO,
each particle is “self-aware” and possesses awareness of
the other members of its population. Each particle knows
its current coordinates and value; its personal best
coordinates and value (pbest); and the overall best
coordinates and value for the global population (gbest).
Each particle constantly adjusts its heading in a direction
toward pbest and gbest.

Particle swarm optimization is attractive because there
are few parameters to adjust, the computer overhead is
low and the technique is easily adapted to a wide variety
of applications. The authors have previously employed
PSO to solve highly nonlinear multidimensional problems
in chemical equilibrium [10]. A generic PSO algorithm is
illustrated in Figure 3.

![Figure 3. Flow of a generic PSO algorithm [9, 11].](image)

A population is seeded with particles, each with
random coordinates. The coordinates of each particle
represent a particular liquid fuel mixture. For example, an
isopentane, toluene, iso-octane mixture might be
represented as Particle (isopentane, toluene, iso-octane).
There are two constraints placed upon particle
coordinates: (a) the mole fraction of each species must be
between 0 and 1, and (b) the sum of a particle’s
coordinates must equal the specified total number of
moles in the mixture. Personal best and global best
coordinates are determined strictly by comparison of the
objective function calculated for each particle.

**METHOD OF APPROACH.**

The PR78 equation of state is as follows [7, 8]:

\[
P_i = \frac{RT}{v_i - b_i} - \frac{a_i}{v_i(v_i + b_i) + b_i(v_i - b_i)}
\]  

\[a_i = 0.45724 \frac{RT^2}{P_{C,i}} \left[ 1 + \kappa_i \left( 1 - \sqrt{\frac{T}{T_{C,i}}} \right)^2 \right]
\]

\[
\kappa_i = \begin{cases} 
0.37464 + 1.5422\omega_i & \text{if } \omega_i \leq 0.491 \\
-0.26992\omega_i^2 & \text{if } 0.491 < \omega_i < 0.763 \\
0.379642 + 1.48503\omega_i & \text{if } \omega_i \geq 0.763
\end{cases}
\]

\[
b_i = 0.07780 \frac{RT_{C,i}}{P_{C,i}}
\]

\[
Z = \frac{PV}{RT}
\]

\[
\ln \phi = (Z - 1) - \ln (Z - B) - \frac{A}{2B\sqrt{2}} \ln \left( \frac{Z + (1 + \sqrt{2})B}{Z + (1 - \sqrt{2})B} \right)
\]

\[A = \frac{aP}{R^2T^2}, \quad B = \frac{bP}{RT}
\]

The Peng-Robinson equation of state (Eq. 1) is
attractive because it allows all relevant parameters to be
calculated from readily available data (critical temperature
ture of the mixture. The goal is to calculate the fugacity ratio (Eq. 4) of the mixture. Fugacity is a measure of the tendency of a substance to migrate to one state over another. The fugacity ratios $\phi$ for liquid and vapor phases are calculated from compressibility factor $Z$ (Eq. 3) at the given temperature and pressure to determine the equilibrium ratio

$$k_\text{eq} = \frac{\phi_\text{vapor}}{\phi_\text{liquid}} = \frac{y}{x},$$

which defines the ratio of the mole fractions of each component in each phase of the mixture. In the equations above, the terms $A$ and $B$ only serve to simplify the compressibility factor equation.

In this first version of the algorithm, ideal gas and ideal liquid behavior are assumed. This greatly reduces computational overhead at the risk of decreased accuracy. However, ideal-behavior-assumption accuracy improves with similarity of the species involved, and is generally acceptable with light hydrocarbons [12, 13]. The constituents of motor gasoline are hydrocarbons in a relatively narrow carbon number range ($C_4$ – $C_{12}$); despite this, should the surrogate mixture include paraffins, olefins, aromatics, air, etc., accuracy can rapidly deteriorate. If necessary, later iteration of the algorithm will consider real-fluid phase equilibrium calculation techniques such as those presented by Jaubert et al [14-18].

Air is assumed to consist only of diatomic nitrogen, due to N$_2$ comprising $\sim$80% of air by volume. The HC concentration of the vapor is a pure HC concentration, meaning that it is just the sum of all the HC molecules, in the vapor, from the different fuels, and not the type of concentration one gets from a HC analyzer that represents HCs in terms of a particular HC (e.g. ppm-C$_1$). The objective function to be minimized was

$$|HC_{\text{target}} - HC_{\text{actual}}|.$$  

HC$_{\text{target}}$ represents the desired HC concentration input into the algorithm, where as HC$_{\text{actual}}$ is the concentration calculated by the algorithm for each fuel composition. The overall convergence criterion was that the absolute value of the difference between these two was within 0.01% of the target concentration.

In order to test the accuracy of the algorithm, its predicted fuel mixtures were input into the NIST SUPERTRAPP [20] vapor-liquid equilibrium program to compare calculated HC concentrations. Tests were conducted with binary, ternary and quinary fuel blends. The target HC concentrations were varied throughout the 2-phase liquid-vapor region of each fuel blend simulated. Also, each fuel blend was tested at 3 different pressures (273, 288 and 300 K) to simulate typical cold start operating conditions. Note, the pressure was held constant at 100,000 Pa throughout all the tests. This was done because the initial pressure for engine starts is typically atmospheric pressure. Each test was conducted five times to check for repeatability. The SUPERTRAPP computations are considered the reference standard here, as its accuracy has been demonstrated in the literature [3, 19].

RESULTS.

Note, for each HC concentration presented in the graphs of this section all represent vapor HC concentrations. Consider the plots given in Figure 4 and Figure 5 of butane/decane and iso-pentane/toluene binary mixtures. Each mixture was simulated at 300 K, 1 bar, and the HC vapor concentration was varied throughout the 2-phase range of the mixture as stated before. For butane/n-decane mixtures, the algorithm agrees very well with SUPERTRAPP, with an average deviation of $\sim$0.56%. With iso-pentane/toluene, the average error is higher, $\sim$7.35%. The larger error is most likely attributable to non-ideal behavior in the paraffin/aromatic blend, vs the paraffin/paraffin butane/decane mixture. Simulations of binary mixtures of butane, iso-pentane, toluene, iso-octane, and n-decane yielded very similar results.
As stated before, each of the mixtures were also tested at 288 K and 273 K. For each mixture, the accuracy of the model decreased with decreasing temperature. For instance, the average deviation for the butane/n-decane mixture is presented in Table 1. It is clear that the accuracy of the algorithm decreases with temperature. All the other mixtures tested exhibited the same exact behavior. This accuracy loss can be directly attributed to the ideal gas/liquid assumptions of the model, which are less valid at lower temperatures, especially at temperatures below the boiling points of all constituents of the mixture, near the mixture single-phase regions.

The next phase of simulation involved quinary fuel mixture, in expectation that any viable gasoline surrogate would require at least five components to accurately model the gasoline distillation profile. The proposed mixture ingredients were butane, isopentane (methylbutane), toluene, iso-octane (2,2,4-trimethylpentane) and decane, chosen because they collectively account for the majority the content of commercially available pump gasoline. Moreover, these components span most of the boiling point range of gasoline. The simulations were conducted for 300 K, 1 bar. Figure 6 shows that there are many possible “recipes” of the given species that can yield a HC concentration of 300,000 ppm. The number of combinations is quite possibly infinite. Verification of the Figure 6 blends is shown in Figure 7, which shows the predicted HC concentration of the algorithm versus that calculated by SUPERTRAPP. Looking at Figure 7, the 5 simulations run at 300,000 ppm all lie very close to the linear trend of the simulations, thus proving all the mixtures predicted by the code are legitimate.

At first glance of Figure 6 the predicted mixtures seem to be somewhat random, which would seem to suggest that the code need be constrained to certain mole number ranges for each species in order to avoid randomness. Closer inspection, however, reveals it seems that species with similar boiling points can be interchanged. In this particular case, the code seems to be swapping the butane and iso-pentane between the different simulations.

<table>
<thead>
<tr>
<th>Table 1. Average deviation of butane/n-decane mixture</th>
</tr>
</thead>
<tbody>
<tr>
<td>Temperature</td>
</tr>
<tr>
<td>Deviation</td>
</tr>
</tbody>
</table>

Figure 5. Predicted HC concentration comparison for isopentane/toluene fuel mixtures.

Figure 6. Quinary mixtures of butane, iso-pentane, toluene, iso-octane, and n-decane. HC concentration is 300,000 ppm.
Further investigating species interchangeability, ternary fuel mixtures were tested with one species of a given boiling point range and the other two species having similar boiling points, e.g., butane, iso-octane and toluene. **Figure 8** (HC target 600,000 ppm) clearly demonstrates the interchangeability of toluene (BP 110.6 °C) and iso-octane (BP 99.3 °C), with verification in **Figure 9**, showing the legitimacy of all the predicted mixtures. Additional simulation revealed that iso-octane, toluene, n-heptane and ethanol were all interchangeable with regard to vaporization. This facilitates choosing surrogate fuel components that most closely approximate ideal behavior.

A simple, yet effective volatility test was devised to verify the predicted species interchangeability. This test – dubbed sTGA – is a simplified version of conventional thermogravimetric analysis. Fuel samples were placed in 4 mL glass cylindrical vials. The sample vials were filled to just below the neck of the bottle, exposing the largest area of liquid possible to air. Because the cross-sectional area of the flask is constant below the neck, the surface area of liquid exposed was constant for the duration of the test. Each sample was opened and placed on a Sartorius A211P microbalance with the sliding glass doors closed for 40 minutes in a 20 °C quiescent atmosphere. The room temperature and sample mass were recorded at two-minute intervals.

sTGA results for the four aforementioned species are shown in **Figure 10**. The mass measurements were normalized by the initial mass for each component. These results confirm virtually identical vaporization rates for iso-octane, toluene, and n-heptane.
Figure 10. Vaporization rates of neat iso-pentane, toluene, n-heptane, and iso-octane

CONCLUSIONS.

A simple phase equilibrium model with the capability of predicting liquid fuel compositions has been demonstrated throughout this work. Although simple, the model has proven very effective and relatively accurate. The model’s accuracy is best when predicting compositions of the same HC family, e.g., paraffins, aromatics, etc. The accuracy decreases when predicting compositions of mixed HC families, such as the isopentane/toluene mixture, which showed an average deviation of ~7.35% when compared NIST’s SUPERTRAPP. Lower temperatures also decrease the model accuracy, which is directly coupled with the ideal gas assumption for the model. These inaccuracies prove the need for incorporating real-fluid behavior, which will be a part of future work. When testing the model, an interesting phenomenon was discovered: with respect to vaporization, species with similar boiling points are interchangeable. The interchangeability of the species was verified experimentally via a test dubbed simplified TGA (sTGA). These results showed that iso-octane, toluene, and n-heptane can be substituted for one another with respect to vaporization. Future work will include characterizing the model’s accuracy against real and simulated engine fuel/air preparation systems.

NOMENCLATURE.

\begin{itemize}
  \item \(v\) specific volume
  \item \(\kappa_i\) constant characteristic for each substance
  \item \(a\) Peng-Robinson EOS attraction parameter
  \item \(b\) Peng-Robinson EOS van der Waals covolume
  \item \(\omega\) acentric factor
  \item \(Z\) compressibility factor
  \item \(\phi_{\text{vapor}}\) fugacity ratio for vapor
  \item \(\phi_{\text{liquid}}\) fugacity ratio for liquid
  \item \(k_{\text{eq}}\) ratio of \(\phi_{\text{vapor}}\) and \(\phi_{\text{liquid}}\)
  \item \(g_{\text{best}}\) global best for particle population
  \item \(i, j\) iteration index
  \item \(P\) pressure
  \item \(P_c\) critical pressure
  \item \(p_{\text{best}i}\) particle personal best
  \item \(R\) universal gas constant
  \item \(T\) temperature
  \item \(T_c\) critical temperature
  \item \(V_i\) particle velocity
  \item \(x_i\) particle position
  \item \(\alpha_{1,2}\) acceleration constants (typically \(\alpha_1 = \alpha_2 = 2\))
  \item \(\gamma_{1,2}\) random velocity and acceleration weight factors \(\in [0,1]\)
  \item \(\phi_i\) particle swarm optimization inertia function
\end{itemize}

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REFERENCES.

INTEGRAL AND REDUCED ORDER LYAPUNOV OPTIMIZING SLIDING MODE CONTROL

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ABSTRACT

Lyapunov optimizing control (LOC) is a powerful control system design methodology that has proven useful for the specification of control laws for linear and nonlinear systems. Lyapunov optimizing control is particularly well suited for optimal control problems; it provides a convenient formulation that introduces cost considerations into the analysis while considering target stability. This is due to the theoretical foundations of LOC which combines Lyapunov stability theory with function minimization. Recently a new LOC algorithm, referred to as Lyapunov Optimizing Sliding Mode Control (LOSMC), was developed. This algorithm maintained the cost and stability characteristics of its LOC predecessors, but in addition developed a cost minimization based methodology for elimination of chatter and disturbance rejection. The contribution of this treatment is to extend LOSMC a step further; to this end an Integral Lyapunov Optimizing Sliding Mode Control algorithm (ILOSMC) is developed. Under the ILOSMC strategy, an integral sliding mode is introduced. Performance will be evaluated in four areas: (1) stability of the origin, (2) accumulated cost, (3) elimination of discontinuous chatter, and (4) disturbance rejection. It is shown that the resulting ILOSMC algorithm, in the presence of a bounded disturbance, provides for the same system behavior as the "candidate control law" (CCL) algorithm in the absence of the disturbance. Therefore a control algorithm may be developed that produces the desired cost, stability, and chatter elimination characteristics provided based on ideal system dynamics, then the addition of an integral sliding surface allows for the same system behavior once a disturbance is present.

INTRODUCTION

Lyapunov’s second method has been used extensively to derive control laws which are effective for controlling linear and nonlinear systems subject to disturbances; two excellent examples being [1,2]. A related method, Lyapunov optimizing control, originated in [3] and produces feedback controls by selecting a candidate Lyapunov function and then choosing a control by minimizing this function as much as possible along system trajectories [4,5]. A useful characteristic of LOC methods comes from a built-in proof of their effectiveness; assuming the target is the origin, if the candidate Lyapunov function is decreased everywhere outside the target, a sufficient condition for asymptotic stability is satisfied [6]. An additional advantage is that the LOC approach provides the analyst a means to design feedback controls where cost accumulation is explicitly considered during the development of the control law. This methodology has been applied to many interesting problems, with [7,8] two such examples. The difficulty in utilizing LOC methods arises during proof of asymptotic stability of the origin. It may be quite difficult to guarantee that the candidate Lyapunov function decreases everywhere outside the origin, especially in the presence of unknown disturbances which are present in any physical system. If the candidate function doesn’t decrease everywhere apart from the origin, stability cannot be proven using traditional Lyapunov analysis.

Sliding Mode Control (SMC) has proven to be an effective approach for the control of uncertain dynamical systems. SMC control algorithms have been shown to reject disturbances and stabilize the origin. These characteristics have made SMC a subject of great interest in terms of both theoretical and practical research [9,10]. The fundamental approach within SMC is to select a switching surface in state space that is attractive, robust to disturbance, and consists of stable dynamics. Once trajectories reach this surface, they “slide” along it and asymptotically approach the origin. A drawback of SMC algorithms is their tendency to induce chatter. Chatter may produce a variety of detrimental effects on a mechanical system such as excitation of resonant modes and extreme wear on actuators. It is typified by high frequency (discontinuous) commutation of the control signal across the sliding surface [11,12].

Integral Sliding Mode Control (ISMC) [13] was developed to ensure the robustness properties of SMC throughout the state
space, rather than on a lower dimensional manifold. This approach (and related methods) has been applied to many interesting problems [14]. This is achieved in part due to the fact that the order of the system motion is equal to the dimension of the state space. Using this method, a satisfactory control law is developed under the assumption that the system is ideal (i.e. no disturbance), then a discontinuous term based on ISMC is added to this to reject the disturbance.

LOSMC [15,16] was developed to address two issues in addition to the cost minimization and stability requirements of an optimal control problem; disturbance rejection and chatter elimination. It was the manner in which these issues were addressed that set it apart from traditional SMC schemes. Specification of a continuous control law (elimination of discontinuous chatter) and placement of the sliding surface (disturbance rejection) were also influenced by cost minimization rather than only stability of reduced order dynamics.

The fundamental contribution of this treatment may be summarized as follows. It is supposed that the CCL has produced desired system behavior, but produces chatter, and does not consider the presence of a disturbance. The idea is to extend the LOSMC methodology; it is shown that introduction of an integral sliding surface to the existing LOSMC algorithm will allow for the same system behavior despite introduction of an “unknown” disturbance. Therefore, a new algorithm ILOSMC, with a cost minimization based rationale, is developed to provide robustness to the disturbance in addition to the stability, cost, and chatter elimination properties of LOSMC and CCL.

This treatment is organized as follows. Section I provides background information on both the problem of interest and existing control methodologies which lead to the development of ILOSMC. Section II details the development of the ILOSMC algorithm. Section III presents a discussion; finally a conclusion is presented to summarize the results.

BACKGROUND INFORMATION

The problem of interest contains both a cost functional that the analyst wishes to minimize and the dynamics which describe the evolution of the state. The cost functional is assumed dependent upon the state and takes the form

\[ J = \int_0^T \Psi(x) \, dt \]  

where

\[ \Psi(x) = \frac{1}{2} x^T \partial^2 \Psi / \partial x^2 x \]  

and the matrix \( \partial^2 \Psi / \partial x^2 \) is constant, symmetric, and positive definite. We consider systems described by linear and nonlinear state equations

\[ \dot{x} = f(x) + b(u + v) \]  

where \( x \in \mathbb{R}^n \) and for the linear case the vector \( f(x) = Ax \). The control variable \( u \) is an element of the constraint set

\[ u_{\min} \leq u \leq u_{\max} \]  

where \( u_{\min} = -|u_{\max}| \) and the unknown disturbance \( v \) satisfies \( |v| < u_{\max} \). The problem (1)-(4) is an optimal control problem; however in the presence of the disturbance \( v \) application of necessary conditions (such as Pontryagin’s minimum principle or dynamic programming) is typically not possible. A different approach is needed.

Quickest Descent Control

In this treatment a particular variant of LOC, known as Quickest Descent Control (QDC), forms the basis for the LOSMC algorithm [15] and the forthcoming ILOSMC algorithm. Controls found by the application of QDC are derived by first selecting a descent function \( \Psi(x) \) that exhibits the following properties [4]:

1) \( \Psi(0) = 0 \)

2) \( \Psi(x) > 0 \) \( \forall x \neq 0 \)

3) \( \frac{d\Psi}{dx} \neq 0 \) \( \forall x \neq 0 \)

The control \( u \) for the problem (1)-(4) is chosen to decrease \( \Psi(x) \) as quickly as possible along trajectories \( x(t) \); therefore, \( u \) is chosen by

\[ \min_u \frac{d\Psi}{dt} \]

In this treatment, we choose the cost accumulation rate \( \Psi(x) \) from (1) as the descent function; that is we let \( \Psi(x) = \Psi(x) \). Therefore, the control \( u \) is chosen as

\[ \min_u \Psi \]  

where \( \Psi = d\Psi/dt \). If \( \Psi < 0 \) \( \forall x \neq 0 \) and properties (1)-(3) are satisfied, then we are assured by Lyapunov’s second method that the origin is asymptotically stable.

Given the cost functional (1) and with the presence of the disturbance \( v \), LOSMC approaches the cost and stability issues by minimizing the rate at which cost accumulates (minimizing \( \Psi \)) and then attempting to prove that the origin is at least locally asymptotically stable. By minimizing this rate of cost accumulation, it is presumed that the final accumulated cost (1) will then be acceptable. Toward this end, several important quantities may be defined [15], including

\[ \Psi = \frac{\partial \Psi}{\partial x} \dot{x} = \frac{\partial \Psi}{\partial x} [f(x) + b(u + v)] \]  

and

\[ \sigma = \frac{\partial \Psi}{\partial x} = \frac{\partial \Psi}{\partial x} b \]  

where \( \sigma = 0 \) is known as the switching surface. Application of first order necessary conditions for the minimization (5) yields the following candidate LOC control law (CCL)
The control $u$ is known as singular control [4]. In optimal control theory, additional necessary conditions are used for its derivation; the final singular control dictates that trajectories travel along the switching surface $\sigma = 0$. Trajectories generated by control laws of the form CCL are well known to chatter about this switching surface, due to the bang-bang (max-min) control effort.

**Sliding Mode Control**

Note that the control law CCL is very similar to traditional sliding mode control [9]. The ability of SMC algorithms to reject disturbances and stabilize the origin has made SMC a subject of great interest [9, 10]. The idea behind SMC is to select a switching surface in state space that is attractive and consists of stable dynamics. Once trajectories reach this surface, they “slide” along it until the origin is reached, despite the unknown disturbance. A drawback of SMC algorithms is their tendency to induce discontinuous chatter which results in high frequency commutation of the control signal across the switching surface [11, 12]. This produces a variety of undesired affects on mechanical systems, including excitation of resonant modes and premature wear on actuators. The originality of the control LOSMC was that the control law is introduced that is discontinuous to provide the desired robustness characteristics.

**Lyapunov Optimizing Sliding Mode Control**

Incorporating the techniques of Trajectory Following Optimization (TFO) into the QDC formulation produces an algorithm that relieves the chatter phenomenon. Chatter is caused by the discontinuous switching of the control $u$ in CCL.

To deal with this, TFO [4] is invoked; this methodology solves optimization problems numerically by defining special sets of differential equations whose equilibrium solutions satisfy first-order necessary conditions [15,16] for the optimization at hand. Using TFO to minimize cost will produce a control effort that is robust to disturbance and alleviates the discontinuous chatter. To this end it was proposed in [15] that the control law be integrated on-line with the dynamic system via

$$\dot{u} = -\frac{1}{\epsilon} \sigma$$

(8)

where $\epsilon$ is a small, positive scalar and the control was allowed to saturate at its upper or lower bounds if (8) would cause a violation of these bounds. The parameter $\epsilon$ may be chosen sufficiently small so that the resulting control $u$, generated by (8) approximates CCL in a continuous manner. Note that the equilibrium solution to (8)

$$\dot{u} = -\frac{1}{\epsilon} \sigma = -\frac{1}{\epsilon} \frac{\partial \Psi}{\partial u}$$

(9)

satisfies a local necessary condition for the minimization of $\Psi$ with respect to $u$. The usefulness of (9) is that we have eliminated the discontinuous chatter caused by CCL by considering minimization of cost. In [16] the control (9) was modified slightly, taking the form

$$\dot{u} = -\frac{1}{\epsilon} \text{sign}(\sigma)$$

(10)

where

<table>
<thead>
<tr>
<th>sign($\sigma$)</th>
<th>-1</th>
<th>if $\sigma(x) &lt; 0$</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>if $\sigma(x) = 0$</td>
<td></td>
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<tr>
<td>1</td>
<td>if $\sigma(x) &gt; 0$</td>
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The advantage of (10) is that the evolution of the control does not become vanishingly small as the state approaches the switching surface $\sigma = 0$, as in (9). The continuous control generated by (10) is the final form of LOSMC.

**INTEGRAL LYAPUNOV OPTIMIZING SLIDING MODE CONTROL**

Integral sliding mode control [9, 10] was developed to ensure robustness of the control algorithm throughout the state space, rather than on a reduced order manifold (sliding surface). This was achieved in large part due to the requirement that the order of the equation of motion under integral sliding mode control is equal to that of the original system. In typical SMC algorithms, the order of motion is reduced by the number of control inputs to the system. Implementation of an integral sliding mode control law is a two-step process. First, it is assumed that the system of interest is ideal; i.e. no disturbance is present in (3). Any desired control law that produces satisfactory system motion is developed. Then, an additional control term is introduced that is discontinuous to provide the desired robustness characteristics.

**ILOS MC Algorithm Derivation**

Derivation of the ILOS MC algorithm will be illustrated by example and will occur in two phases. For each example, one linear and one nonlinear, a CCL controller will be developed under the assumption of ideal system dynamics. Then, an integral sliding surface will be incorporated into the LOSMC control law. System behavior under the resulting ILOS MC algorithm (subject to a nonlinear disturbance) will be compared to system behavior under CCL algorithm (with the assumption of ideal dynamics) via simulation. During the derivation of the ILOS MC algorithm it is important to consider the nature of the physical system to which it is applied. Future directions and applications will be detailed in the conclusion.

**Linear Example**

Consider a mechanical system represented by the state equations

$$\dot{x}_1 = x_2$$

$$\dot{x}_2 = x_1 + 4x_2 + u + \nu$$

(11)
In this research effort, it was supposed that equations of the form (11) were derived from Newton’s laws (rotational motion). The physical system could represent a simple robot end-effector, or the classic problem of stabilizing a linearized, inverted pendulum. In this scenario, \( x_1 \) represents the angular position of the pendulum, and \( x_2 \) the angular acceleration. The first two terms on the right hand side of the second of equations (11) could represent gravitational and frictional torque. The variable \( u \) is a control torque, and the variable \( v \) a disturbance (wear in the bearings). This scenario frames the typical applications to which ILOSMC is currently applied.

As described in equations (1)-(3), the ILOSMC addresses the optimal control problem. It is assumed that the analyst has identified a cost function which depends on the system states. For this example, the total accumulated cost is given by (1) with

\[ \Psi(x) = x_1^2 + x_1 x_2 + 3x_2^2 \]

While this equation and (1) are general, they could represent system mechanical energy or specific angular position and speed constraints on the system.

To begin, the LOSMC control law is designed by first assuming ideal dynamics \((v = 0)\) in (11), and calculating the instantaneous rate at which cost accumulates, \( \Psi \). Therefore,

\[ \dot{\Psi} = \frac{\partial \Psi}{\partial x} \dot{x} = \left[ 2x_1 + x_2 \ x_1 + 6x_2 \right] \left[ \frac{x_2}{x_1 + 4x_2 + u} \right] \]  
(12)

and

\[ \sigma = \frac{\partial \Psi}{\partial u} = x_2 + 6x_2 \]  
(13)

It may be seen by inspection of (12) and (13) that the instantaneous rate of cost accumulation may be minimized by the control law CCL. However, this type of control which switches discontinuously across the switching surface, \( \sigma = 0 \), is notorious for producing chatter. This phenomenon is illustrated in Figs. 1 and 2.

\[ \dot{u} = -\epsilon \text{sign}(\sigma) \]  
(14)

where the control is allowed to saturate as necessary at its upper and lower bounds. Consider equations (5)-(10) in view of (14). If the gradient \( \partial \Psi/\partial u \) is not zero, (14) acts to make \( \Psi \) more negative. This persists unless \( \partial \Psi/\partial u = 0 \) which is exactly the first order necessary condition for minimizing \( \Psi \) with respect to \( u \). Therefore, chattering is addressed because the control action is continuous while considering the minimization of \( \Psi \) in tandem.

An integral sliding mode controller is typically developed by adding a two-part control to any control law that has produced desired system behavior in the absence of a disturbance [13]. For the purposes of this treatment, we assume that the control

Note that in this treatment, the output is taken as the state. For example, suppose equations (11) represent the angular position \((x_1)\) and angular velocity \((x_2)\) of a robot end-effector. We are interested in driving those states to the origin thereby dictating a preferred position in state space despite any disturbance. For the initial conditions presented, \( x_1(0) = +/- 0.8 \) and \( x_2(0) = 0 \), the origin is asymptotically stable (proofs of this may be found in [15]) and the control was chosen at each instant to minimize \( \Psi \). Control bounds were set at \( u_{\text{max}} = 2.5 \). The high frequency commutation of the control, implemented by any physical actuator, would cause system wear and may excite resonant modes.
law CCL has produced satisfactory system behavior (we will address chatter again shortly). Then an additional control term is added; see [13] for a detailed derivation of the integral sliding mode method. This additional term consists of two parts; the first, $z_0$, is a discontinuous term that is provided to reject an unknown (but bounded) disturbance. The second portion, $z_1$, creates the integral sliding surface. An integral sliding surface allows for the sliding motion to be enforced throughout the state space, rather than on a reduced order (linear) surface. Therefore, algorithms invoking the integral sliding mode technique may be insensitive to disturbance throughout the state space, rather than just on the linear sliding surface [13]. The contribution of the integral methodology to the overall control law takes the form

$$z = z_0 + z_1$$

where $z_0$ is a linear combination of system states and $x_3$ obeys the differential equation

$$\dot{z}_1 = -\left[\frac{\partial z_0}{\partial x_1} x_1 + \frac{\partial z_0}{\partial x_2} x_2 + \frac{\partial z_0}{\partial u} u\right]$$

with the initial condition $z_1(0) = -z_0(x(0))$ a detailed explanation of (15, 16) is contained in [13] and is omitted here. Note that the term $z_1$ is integrated on-line and makes the order of motion on the sliding mode equal to that of the original system. This is the definition of an integral sliding mode.

However, a primary goal of this treatment is to eliminate chatter from an optimization point of view. Therefore the ILOSMC is designed by developing a continuous control effort that incorporates the original LOSMC control effort (CCL) and augments it with the integral control effort. We propose to augment (14) with a term that enforces the integral sliding surface (15). This includes selecting

$$z_0 = \sigma$$

(this choice will be discussed further in the next section) and finally letting

$$\dot{u} = -\frac{1}{\epsilon} \text{sign}(\sigma) - \frac{1}{\epsilon} \text{sign}(z)$$

(18)

This choice means that the controlled system, under the ILOSMC regime, obeys the differential equations (3) where the “$u$” term is generated on-line by (18).

It is important to discuss the control bounds here. The control law CCL was developed under the assumption that no disturbance was present. Furthermore, the control bounds on (18) at saturation were allowed to be greater than that for the system acting under ideal dynamics (14). The justification for this is that we have enacted a control law that produces appropriate behavior under ideal dynamics. Additional control effort (actuators) are needed to replicate this in the presence of a disturbance. The ideal control effort was limited to $u_{\text{max}} = 2.5$ while in the presence of the disturbance a total of $u_{\text{max}} = 7.5$ control effort was available. That is, both $\sigma$ and $z$ had available control effort that could potentially switch (or saturate) independently.

Figures 3 and 4 illustrate the behavior of the system (11) under the ILOSMC algorithm (18) to performance under CCL without the disturbance. For the purpose of this example, the “unknown” disturbance was taken to be $v = \cos(t)$. The initial conditions were taken to be $x_1(0) = \pm 0.8$ and $x_2(0) = 0$; Fig. 3 is a “wide” view where, for clarity, only the CCL trajectories are shown. Figure 4 is a “close” view where both ILOSMC and CCL trajectories may be seen. It is clearly seen that the ILOSMC closely mimics the behavior of the CCL controlled system (without disturbance) and does so in a continuous manner, thereby eliminating chatter. Note that the amplitude of the ILOSMC response in Fig. 4 is greater than that of CCL. Several factors contribute to this. The ILOSMC algorithm is continuous; it is well known that discontinuous...
control algorithms (such as CCL) react quicker to the presence of a disturbance. However, the slight deviation from this behavior is acceptable due to avoidance of the chatter phenomena. Also, when implementing the continuous control law ILOSMC, the integration step size and the gain $(1/\varepsilon)$ effect the system response. These factors will contribute to the discrepancy between the ILOSMC and CCL responses shown in Fig. 4.

**Nonlinear Example**

Consider a mechanical system represented by the state equations

\[ \begin{align*}
    \dot{x}_1 &= x_2 \\
    \dot{x}_2 &= x_1^2 - \sin(x_2) + u + v
\end{align*} \]  

And again assume again that the total accumulated cost is given by (1) with

\[ \Psi(x) = x_1^2 + x_1 x_2 + 3x_2^2 \]

The LOSMC control law is designed by first assuming ideal dynamics ($v = 0$) in (19), and calculating the instantaneous rate at which cost accumulates, $\Psi$. Therefore,

\[ \Psi = \frac{\partial \Psi}{\partial x} \mathbf{x} = \left[ 2x_1 + x_2 \left. x_1 - \sin(x_2) + u \right] \]

and

\[ \sigma = \frac{\partial \Psi}{\partial \mathbf{x}} = x_1 + 6x_2 \]

As in the linear example, it may be seen by inspection of (20) and (21) that the instantaneous rate of cost accumulation may be minimized by the control law CCL. Again, this type of control which switches discontinuously across the switching surface, $\sigma = 0$, is notorious for producing chatter. The techniques of TFO are implemented to mitigate this phenomenon. We desire to execute the minimization (5), as before we select

\[ \dot{u} = -\frac{1}{\varepsilon} \text{sign}(\sigma) = -\frac{1}{\varepsilon} \text{sign}(x_1 + 6x_2) \]  

where the control is allowed to saturate as necessary at its upper and lower bounds. The ILOSMC development follows as for the linear example yielding the augmented form,

\[ \dot{u} = -\frac{1}{\varepsilon} \text{sign}(\sigma) - \frac{1}{\varepsilon} \text{sign}(v) \]

The controlled system, under the ILOSMC regime, obeys the differential equations (3) where the “$u$” term is generated online by (23). Figures 5 and 6 illustrate the behavior of the system (19) under the ILOSMC algorithm (23) to performance under CCL without the disturbance. The disturbance was taken to be $v = \cos(t)$. To illustrate that ILOSICM is robust to non-smooth disturbances additional simulations were executed where $v$ randomly assumed values between -1 and 1. Figures 7 and 8 verify that ILOSICM produced the desired stability and disturbance rejection results.

**DISCUSSION**

A useful result of this treatment was to show that it is possible to blend stability and cost considerations to develop a controller that mimics “ideal” behavior; that is rejects an unknown disturbance.

Two distinct examples, one linear and one nonlinear, were considered. In both examples (for consistency and clarity) the disturbance was taken to be a cosine wave and the initial switching surfaces were identical. Figures 1 and 2 illustrate the chattering behavior which we desire to eliminate. Figures 3 and 5 (linear and nonlinear) illustrate the general behavior (broad view, CCL only) with Figs. 4 and 6 comparing system behavior under ILOSMC and CCL. It is clear that the discontinuous chatter has been eliminated; furthermore, no real device can switch instantaneously fast. Additionally, the nonlinear system was subject to a discontinuous disturbance; which was rejected by the algorithm.
Two primary contributions of this treatment may now be discussed. First, to the best of our knowledge this is the first application of integral sliding mode concepts within LOC. Since the LOC methodology combines function minimization with asymptotic stability, ILOSMC is a simple to apply, robust control law, derived from an optimization point of view. Secondly, a particularly interesting contribution comes from the manner in which the integral sliding surface \( z \) is specified. We have provided an optimization based methodology for choosing \( z_0 = \sigma \). The justification is exhibited in Fig. 9; when minimizing the defined cost, such a choice should be made. With combined control effort (18) and (23) saturating and switching sign “together”, the overall accumulated cost is minimized. Note that for the initial conditions presented, the control was able to produce a negative instantaneous rate of change of cost (thus the negative accumulated cost).

**CONCLUSION**

Any developed control law must confront the fact that disturbances are always present within a physical system. The purpose of this treatment was to attempt to design a control law that could very closely replicate the behavior of an ideal system, under the influence of a suitably chosen control law. The development of ILOSMC (Section II) and the comments in the Discussion (Section III) show that this was achieved. In particular, the final ILOSMC control law was able to consider cost, asymptotic stability of the origin, elimination of discontinuous chatter, and disturbance rejection. One particular aspect that makes this treatment novel is that these desired objectives were achieved through consideration of the underlying optimization. An area of particular interest for future research is related to the specification of the “unknown” disturbance \( v \). In particular, suppose that the disturbance is a second “player” who is interested in minimizing its own cost functional. Such a scenario could be called an instantaneous game [4] where worst case behavior (from the viewpoint of the “u” player) could be considered. The performance, in terms of cost and stability, in a worst case scenario would be illuminated. A second area of future research would be application of ILOSMC to a more complex industrial robot. The state equations for this treatment were used to derive and present the algorithm. However, a more complex system of differential equations that represent the dynamic behavior of a multi-DOF industrial robot will be pursued next.

**REFERENCES**


GLOSSARY

(LOC) Lyapunov Optimizing Control
A control methodology based upon asymptotic stability of the origin and function minimization. Applicable to optimal control problems.

(LOSMC) Lyapunov Optimizing Sliding Mode Control
A control methodology where application of LOC produces a reduced order sliding surface.

(ILOSMC) Integral Lyapunov Optimizing Sliding Mode Control
An extension of LOSMC where an integral sliding regime is invoked.

(CCL) Candidate Control Law
A typical control law produced by the quickest descent variant of the LOC methodology.

(SMC) Sliding Mode Control
A powerful, robust, control methodology which may also produce discontinuous chatter.

(QDC) Quickest Descent Control
A special case of the LOC control methodology.

(TFO) Trajectory Following Optimization
A continuous time optimization methodology; used in this treatment to alleviate discontinuous chatter.
THE USE OF TAGUCHI QUALITY LOSS FUNCTION TO OPTIMIZE MULTIPLE QUALITY CHARACTERISTICS IN LASER TRANSMISSION WELDING PROCESS OF THERMOPLASTICS

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ABSTRACT  
This paper presents and demonstrates the effectiveness of optimizing multiple quality characteristics in laser transmission welding process using Taguchi quality loss function. The multiple signal-to-noise ratios obtained from the Taguchi quality loss function analysis are used to solve the laser transmission welding process with the multiple quality characteristics. Optimal welding parameters can be determined by the multiple signal-to-noise ratios as the quality index. The two quality characteristics, weld strength and weld width, that are of different nature (weld strength is of the higher-the-better type, while weld width is of the lower-the-better type), are selected for simultaneous optimization. The results show considerable improvement in both the quality characteristics with this optimization technique.

INTRODUCTION  
Laser transmission welding of thermoplastics is a modern and innovative technology with well-known general advantages of laser materials processing, as a non-contact, non-contaminant process, flexible, easy to control and automate. In this laser welding process a laser beam is passed through one transparent material and directly heating only the second material, precisely at the mating surface. With this technique, melt is only created where it is needed, in the joining area of the both partners, reducing the energy input to a minimum [1,2]. The shape and strength of weld seam are strongly dependent of the polymer optical properties and process parameters. The most important process parameters of the laser transmission welding are laser power, welding speed, size of laser beam on the work-piece and clamping pressure. These parameters control the temperature field inside the weld seam during welding [3]. Particular care is required to prevent degradation, oxidation and burning phenomenon. In order to get the desired weld quality the combination of the process parameters should be selected carefully. Therefore, it is important to determine the process parameters at which the response reaches its optimum.

The Taguchi method [4,5] is a systematic application of design and analysis of experiments for the purpose of designing and improving product quality. In recent years, the Taguchi method has become a powerful tool for improving productivity during research and development so that high quality products can be produced quickly and at low cost. However, the original Taguchi method is designed to optimize a single quality characteristic. Furthermore, optimization of multiple quality characteristics is much more complicated than optimization of a single quality characteristic [6,7]. Improving one particular quality characteristic would possibly lead to serious degradation of the other critical quality characteristics. When the results have a conflict between multiple quality characteristics, it is necessary to rely on the subjective experiences of engineers to attain a compromise [8]. As a result, uncertainty will be increased during the decision-making process. Antony [9] has demonstrated an alternative approach for tackling such optimization problem using Taguchi quality loss function analysis by taking an example of electronic assembly problem. He has found considerable improvement in multiple quality characteristics, in comparison to single quality characteristics. Further, Aslan [10] has applied Taguchi quality loss function for optimization of grade...
and recovery for chromite concentration with multi-gravity separator. The results show considerable improvement in both the quality characteristics with multi-objective optimization, compared to the initial value of grade and recovery.

In this paper, the use of the Taguchi quality loss function to optimize the laser transmission welding process with consideration of multiple weld qualities such as weld strength and weld width is presented. In the following, the overview of multi response optimization technique based on Taguchi quality loss function is given first. Then, the selection of laser transmission welding process parameters and the evaluation of weld qualities are discussed. After that, optimal process parameters with consideration of multiple quality characteristics are obtained and verified. Finally, the paper concludes with a summary of this study.

TAGUCHI QUALITY LOSS FUNCTION FOR OPTIMIZATION OF PROCESS PARAMETERS

The Taguchi method of robust parameter design is an offline statistical quality control technique in which the level of controllable factors or input process parameters are so chosen to nullify the variation in responses due to uncontrollable or noise factors such as humidity, vibration and environmental temperature. In this method, the experiments are performed as per specially designed experimental matrix known as orthogonal array [8,11]. The experimental values of the quality characteristics are used to compute the quality loss values for each quality characteristic in all experimental runs. Quality loss is the loss associated with a product owing to the deviation in the functional performance of the product from its target. Depending upon the nature of the quality characteristics, the quality loss function can be of several types (lower-the-better, higher-the-better, and nominal-the-best). In the present case the higher weld strength and lower weld width is desired. Therefore, the higher-the-better weld strength and lower-the-better weld width is selected.

The quality loss function, \( l_i \), of the higher-the-better quality characteristic can be expressed as:

\[
I_i = \left( \frac{1}{n} \sum_{j=1}^{n} \frac{1}{y_i^*} \right)
\]

where \( y_i \) is the observed data (or quality characteristic) at the \( i \)th trial of the same parameter level, and \( n \) is the number of trials.

The loss function of the lower-the-better quality characteristic can be expressed as:

\[
I_i = \left( \frac{1}{n} \sum_{j=1}^{n} y_i^2 \right)
\]

For simultaneous optimization of more than one quality characteristic it is required to compute the normalized quality loss, because the unit of each quality characteristics is not the same. The normalized quality loss can be computed using [10]:

\[
L_i^* = \frac{l_i^*}{l_0^*}
\]

where \( l_i^* \) is normalized quality loss for the \( i \)th quality characteristic at the \( j \)th trial condition, and \( l_0^* \) is the maximum quality loss for the \( i \)th quality characteristic among all the trial conditions. It is important to note that \( l_i^* \) varies from a minimum of zero to a maximum of one.

For computing the total normalized quality loss (TNQL), \( L_i \), corresponding to each trial condition, one must assign a weighting factor for each quality characteristic considered in the optimization process. If \( w_i \) represents the weighting factor for the \( i \)th quality characteristic, \( k \) is the number of quality characteristics and \( l_i^* \) is normalized quality loss associated with the \( i \)th quality characteristic at the \( j \)th trial condition, then \( L_j \) can be computed using [9]:

\[
L_j = \sum_{i=1}^{k} w_i l_i^*
\]

After the total normalized quality loss corresponding to each trial condition is calculated, the next step is to compute the multiple signal-to-noise ratio (MSNR) at each design point. The MSNR corresponding to the \( j \)th trial condition, \( \eta_j \), is calculated as:

\[
\eta_j = -10 \log_{10} (L_j)
\]

The aim is always to maximize the MSNR. The average value of all MSNR, when a process parameter is at the same distinct level, is used to describe the effect of a process parameter or factor on the quality characteristics at that level [10]. A parameter level corresponding to the maximum average MSNR is called the optimum level for that parameter. Finally, a verification experiment is conducted at the suggested optimum parameter levels to confirm the predicted response.

EXPERIMENTAL WORK

Materials and the laser welding system

In this work, natural and opaque (added 0.25 % wt. carbon black as color pigment) acrylic plaques are used as the work materials for laser transmission welding, which are of dimensions of 70 mm length, 35 mm width and 4 mm thickness, cut from injection moulded acrylic sheets of size 2000 mm x 1500 mm x 4 mm. Lap joint configuration is only considered in this experiment. A welding fixture is used for repeating work, to maintain the lapping area constant, 20 mm x 35 mm, for every run and to prevent misalignment between the parts to be welded in lap joint geometry.
A continuous diode laser system is used for the experimental investigations. The system installation consists of a 30 W Coherent FAP diode laser with a 3-axes CNC work table, coordinated with the motion system and computer interface, as shown in Figure 1. The laser beam coming out from the oscillator is transferred through an optical fiber with 800 µm of the core diameter, and finally focused by the focusing lens of 33 mm effective focal length. The laser system has an operating wavelength of 809.4 nm. Hydraulic clamp pressure is applied in between the workpieces to ensure the intimate contact between them. The pressure applied to the workpieces is determined from the reading of the pressure gauge, fitted to the hydraulic pump, converted to the pressure experienced by the plaques based on the actual area of contact between the overlapped sections of each sample. The contour welding variant of laser transmission welding is adopted for this study.

**Figure 1. Pictorial view of experimental setup**

**Selection of Welding Parameters**

The following independently controllable process parameters are identified to carry out the experiments: power, welding speed and focal distance, while keeping clamp pressure at a constant value of 3 MPa. Trial runs are conducted by varying one of the process parameters at a time while keeping the rest of them at constant value. The working range is decided by inspecting the weld seam for a smooth appearance and the absence of any visible defects. The selected process parameters and their levels, units and symbols are given in Table 1.

**Evaluation of Quality Characteristics**

All the welded specimens are tested for their relative strengths under tension using an Instron universal testing machine. The crosshead speed during the pull test is kept constant at 0.5 mm/min. The weld strength is calculated as the maximum load to failure per unit length of the weld. All the welded specimens are observed under Olympus STM6 optical microscope for measuring the weld width. The weld width for each of the specimens is measured at the center of the seam length. For each parameters setting the average of at least three results of both weld strength and weld width are calculated.

**Table 1. Laser transmission welding parameters and their levels**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Level</th>
</tr>
</thead>
<tbody>
<tr>
<td>$P$ : Power, watt</td>
<td>1</td>
</tr>
<tr>
<td>$S$ : Welding speed, mm/min</td>
<td>20</td>
</tr>
<tr>
<td>$F$ : Focal distance, mm</td>
<td>+6</td>
</tr>
</tbody>
</table>

**DETERMINATION OF OPTIMAL WELDING PARAMETERS**

The methodology of Taguchi for three-factors at four-levels is used for the implementation of the plan of orthogonal array experiments. An L16 orthogonal array with three columns and sixteen rows is employed in this work. The experiments are carried out according to the arrangement of the orthogonal array. The experimental layout for the welding process parameters using the L16 orthogonal array and the experimental results are presented in Table 2.

**Table 2. Experimental lay out and results**

<table>
<thead>
<tr>
<th>Exp. no.</th>
<th>Welding parameters</th>
<th>Weld strength (N/mm)</th>
<th>Weld width (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$P$ (W) $S$ (mm/min) $F$ (mm)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>1</td>
<td>1 1 1</td>
<td>31.44</td>
<td>2.13</td>
</tr>
<tr>
<td>2</td>
<td>1 2 2</td>
<td>38.00</td>
<td>2.16</td>
</tr>
<tr>
<td>3</td>
<td>1 3 3</td>
<td>33.74</td>
<td>2.17</td>
</tr>
<tr>
<td>4</td>
<td>1 4 4</td>
<td>26.74</td>
<td>2.13</td>
</tr>
<tr>
<td>5</td>
<td>2 1 2</td>
<td>47.27</td>
<td>2.37</td>
</tr>
<tr>
<td>6</td>
<td>2 2 1</td>
<td>38.08</td>
<td>2.16</td>
</tr>
<tr>
<td>7</td>
<td>2 3 4</td>
<td>39.23</td>
<td>2.36</td>
</tr>
<tr>
<td>8</td>
<td>2 4 3</td>
<td>35.02</td>
<td>2.09</td>
</tr>
<tr>
<td>9</td>
<td>3 1 3</td>
<td>57.95</td>
<td>2.71</td>
</tr>
<tr>
<td>10</td>
<td>3 2 4</td>
<td>51.17</td>
<td>2.64</td>
</tr>
<tr>
<td>11</td>
<td>3 3 1</td>
<td>41.45</td>
<td>2.11</td>
</tr>
<tr>
<td>12</td>
<td>3 4 2</td>
<td>39.50</td>
<td>2.03</td>
</tr>
<tr>
<td>13</td>
<td>4 1 4</td>
<td>48.85</td>
<td>2.67</td>
</tr>
<tr>
<td>14</td>
<td>4 2 3</td>
<td>56.18</td>
<td>2.51</td>
</tr>
<tr>
<td>15</td>
<td>4 3 2</td>
<td>49.51</td>
<td>2.29</td>
</tr>
<tr>
<td>16</td>
<td>4 4 1</td>
<td>34.38</td>
<td>2.05</td>
</tr>
</tbody>
</table>

The quality loss values of weld strength and weld width in each experimental run are calculated using Equations (1) and (2). Table 3 presents the calculated...
quality loss values for weld strength and weld width. The normalized quality loss values for both the quality characteristics in each experimental run are calculated using Equation (3) that is shown in Table 4. Table 5 presents the results of total normalized quality loss (TNQL) and multiple signal-to-noise ratio (MSNR) which are calculated using Equations (4) and (5). In calculating TNQL the weighting ratio for both the quality characteristics are set as 1:1, i.e., each characteristic has equal importance or relative weighting.

Table 3. Quality loss values for weld strength and weld width

<table>
<thead>
<tr>
<th>Exp. no.</th>
<th>Quality loss value (dB)</th>
<th>Weld strength</th>
<th>Weld width</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>0.0010</td>
<td>4.5156</td>
<td></td>
</tr>
<tr>
<td>2</td>
<td>0.0007</td>
<td>4.6816</td>
<td></td>
</tr>
<tr>
<td>3</td>
<td>0.0009</td>
<td>4.6998</td>
<td></td>
</tr>
<tr>
<td>4</td>
<td>0.0014</td>
<td>4.5280</td>
<td></td>
</tr>
<tr>
<td>5</td>
<td>0.0004</td>
<td>5.6136</td>
<td></td>
</tr>
<tr>
<td>6</td>
<td>0.0007</td>
<td>4.6716</td>
<td></td>
</tr>
<tr>
<td>7</td>
<td>0.0006</td>
<td>5.5852</td>
<td></td>
</tr>
<tr>
<td>8</td>
<td>0.0008</td>
<td>4.3572</td>
<td></td>
</tr>
<tr>
<td>9</td>
<td>0.0003</td>
<td>7.3446</td>
<td></td>
</tr>
<tr>
<td>10</td>
<td>0.0004</td>
<td>6.9685</td>
<td></td>
</tr>
<tr>
<td>11</td>
<td>0.0006</td>
<td>4.4382</td>
<td></td>
</tr>
<tr>
<td>12</td>
<td>0.0006</td>
<td>4.1144</td>
<td></td>
</tr>
<tr>
<td>13</td>
<td>0.0004</td>
<td>7.1508</td>
<td></td>
</tr>
<tr>
<td>14</td>
<td>0.0003</td>
<td>6.2981</td>
<td></td>
</tr>
<tr>
<td>15</td>
<td>0.0004</td>
<td>5.2629</td>
<td></td>
</tr>
<tr>
<td>16</td>
<td>0.0008</td>
<td>4.2062</td>
<td></td>
</tr>
</tbody>
</table>

Table 4. Normalized quality loss values for weld strength and weld width

<table>
<thead>
<tr>
<th>Exp. no.</th>
<th>Normalized quality loss value</th>
<th>Weld strength</th>
<th>Weld width</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>0.7225</td>
<td>0.6148</td>
<td></td>
</tr>
<tr>
<td>2</td>
<td>0.4947</td>
<td>0.6374</td>
<td></td>
</tr>
<tr>
<td>3</td>
<td>0.6274</td>
<td>0.6399</td>
<td></td>
</tr>
<tr>
<td>4</td>
<td>0.9986</td>
<td>0.6165</td>
<td></td>
</tr>
<tr>
<td>5</td>
<td>0.3197</td>
<td>0.7643</td>
<td></td>
</tr>
<tr>
<td>6</td>
<td>0.4926</td>
<td>0.6361</td>
<td></td>
</tr>
<tr>
<td>7</td>
<td>0.4642</td>
<td>0.7604</td>
<td></td>
</tr>
<tr>
<td>8</td>
<td>0.5826</td>
<td>0.5933</td>
<td></td>
</tr>
<tr>
<td>9</td>
<td>0.2127</td>
<td>1.0000</td>
<td></td>
</tr>
<tr>
<td>10</td>
<td>0.2728</td>
<td>0.9488</td>
<td></td>
</tr>
<tr>
<td>11</td>
<td>0.4158</td>
<td>0.6043</td>
<td></td>
</tr>
<tr>
<td>12</td>
<td>0.4575</td>
<td>0.5602</td>
<td></td>
</tr>
<tr>
<td>13</td>
<td>0.2993</td>
<td>0.9736</td>
<td></td>
</tr>
<tr>
<td>14</td>
<td>0.2263</td>
<td>0.8575</td>
<td></td>
</tr>
<tr>
<td>15</td>
<td>0.2914</td>
<td>0.7166</td>
<td></td>
</tr>
<tr>
<td>16</td>
<td>0.6043</td>
<td>0.5727</td>
<td></td>
</tr>
</tbody>
</table>

Table 5. Total normalized quality loss (TNQL) and multiple signal-to-noise ratios (MSNR)

<table>
<thead>
<tr>
<th>Exp. no.</th>
<th>TNQL</th>
<th>MSNR (dB)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>0.6687</td>
<td>0.1748</td>
</tr>
<tr>
<td>2</td>
<td>0.5660</td>
<td>0.2472</td>
</tr>
<tr>
<td>3</td>
<td>0.6336</td>
<td>0.1982</td>
</tr>
<tr>
<td>4</td>
<td>0.8076</td>
<td>0.0928</td>
</tr>
<tr>
<td>5</td>
<td>0.5420</td>
<td>0.2660</td>
</tr>
<tr>
<td>6</td>
<td>0.5643</td>
<td>0.2485</td>
</tr>
<tr>
<td>7</td>
<td>0.6123</td>
<td>0.2130</td>
</tr>
<tr>
<td>8</td>
<td>0.5879</td>
<td>0.2307</td>
</tr>
<tr>
<td>9</td>
<td>0.6063</td>
<td>0.2173</td>
</tr>
<tr>
<td>10</td>
<td>0.6108</td>
<td>0.2141</td>
</tr>
<tr>
<td>11</td>
<td>0.5100</td>
<td>0.2924</td>
</tr>
<tr>
<td>12</td>
<td>0.5088</td>
<td>0.2934</td>
</tr>
<tr>
<td>13</td>
<td>0.6365</td>
<td>0.1962</td>
</tr>
<tr>
<td>14</td>
<td>0.5419</td>
<td>0.2661</td>
</tr>
<tr>
<td>15</td>
<td>0.5040</td>
<td>0.2976</td>
</tr>
<tr>
<td>16</td>
<td>0.5885</td>
<td>0.2303</td>
</tr>
</tbody>
</table>

The effect of each welding parameter on the MSNR at different levels can be separated out because the experimental design is orthogonal. The response of the MSNR for each level of the welding process parameters is summarized and shown in Table 6. In addition, the total mean of the MSNR for the sixteen experiments is also calculated and listed in Table 6. Figure 2 shows the effect of factor levels on MSNR, where the dashed line in this figure is the value of the total mean of the MSNR. Basically, the larger the MSNR, the better are the multiple performance characteristics. Based on the Figure 2, the optimal welding process parameters are determined as laser power at level 3, welding speed at level 3 and focal distance at level 2, i.e., $P_3, S_3, F_2$.

In order to identify the significance of each factor, the use of analysis of variance (ANOVA) is performed. This is accomplished by separating the total variability of the MSNR, which is measured by the sum of squared deviations from the total mean of the MSNR, into contributions by each welding process parameter and the error. The ANOVA results are illustrated in Table 7. This process is carried out by comparing the $F$-test value of the parameter with the standard $F$ table value ($F_{0.05}$) at the 5% significance level. If the $F$-test value is greater than $F_{0.05}$, the process parameter is considered significant. From the ANOVA table, it can be seen that all the process parameters are statistically significant. The same table also shows the percentage contribution of each process parameter to the total variation, indicating their degree of influence on the MSNR. According to ANOVA results, focal distance (49.61%) has the most dominant effect on total variation and it is followed by laser power (37.99%) and welding speed (12.40%).
Table 6. Response table for multiple signal-to-noise ratios

<table>
<thead>
<tr>
<th>MSNR</th>
<th>Welding parameters</th>
<th>P</th>
<th>S</th>
<th>F</th>
</tr>
</thead>
<tbody>
<tr>
<td>Level 1</td>
<td></td>
<td>0.1783</td>
<td>0.2136</td>
<td>0.2366</td>
</tr>
<tr>
<td>Level 2</td>
<td></td>
<td>0.2396</td>
<td>0.2440</td>
<td>0.2760</td>
</tr>
<tr>
<td>Level 3</td>
<td></td>
<td>0.2543</td>
<td>0.2503</td>
<td>0.2280</td>
</tr>
<tr>
<td>Level 4</td>
<td></td>
<td>0.2476</td>
<td>0.2119</td>
<td>0.1791</td>
</tr>
<tr>
<td>Delta</td>
<td></td>
<td>0.0244</td>
<td>0.0204</td>
<td>0.0461</td>
</tr>
<tr>
<td>Rank</td>
<td></td>
<td>2</td>
<td>3</td>
<td>1</td>
</tr>
</tbody>
</table>

Total mean value of the MSNR = 0.2285

Table 7. Results of the ANOVA

<table>
<thead>
<tr>
<th>Source</th>
<th>dof</th>
<th>SS</th>
<th>MS</th>
<th>F</th>
<th>Contribution (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>P</td>
<td>3</td>
<td>0.0147</td>
<td>0.0049</td>
<td>15.57</td>
<td>37.99</td>
</tr>
<tr>
<td>S</td>
<td>3</td>
<td>0.0048</td>
<td>0.0016</td>
<td>5.13</td>
<td>12.40</td>
</tr>
<tr>
<td>F</td>
<td>3</td>
<td>0.0191</td>
<td>0.0064</td>
<td>20.23</td>
<td>49.61</td>
</tr>
<tr>
<td>Error</td>
<td>6</td>
<td>0.0019</td>
<td>0.0003</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Total</td>
<td>15</td>
<td>0.0404</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

dof = Degrees of freedom, SS = Sum of squares, MS = Mean square, F = Fisher’s ratio

CONFIRMATION EXPERIMENT

Once the optimal level of the welding parameters is selected, the final step is to predict and verify the improvement of the quality characteristics using the optimal level of the welding parameters. The estimated MSNR, \( \eta_{opt} \), using the optimal level of the welding parameters is calculated as:

\[
\eta_{opt} = \bar{\eta} + \sum_{i=1}^{k}(\eta_i - \bar{\eta}_n)
\]  

where, \( \bar{\eta}_n \) is the total mean MSNR of all the experimental values, \( \eta_i \) is the mean MSNR at the optimal level, and \( k \) is the number of welding parameters that significantly influence the quality characteristics.

The experiment is performed by conducting a test with optimal setting of the welding parameters and levels previously calculated. The predicted value of MSNR and that from confirmation test are shown in Table 8. The improvement in MSNR at the optimum level is found to be 0.1042 dB. The weld strength and weld width value at the optimum level are 46.68 N/mm and 2.16 mm, respectively, against the initial parameter setting of 39.23 N/mm and 2.36 mm. It is shown clearly that the weld strength and the weld width are greatly improved through this study.

Table 8. Result of confirmation experiment

<table>
<thead>
<tr>
<th>Level</th>
<th>Initial condition</th>
<th>Optimal condition</th>
<th>Prediction</th>
<th>Experiment</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>( P_2S_3F_2 )</td>
<td>( P_3S_3F_2 )</td>
<td>( P_3S_3F_2 )</td>
<td>( P_3S_3F_2 )</td>
</tr>
<tr>
<td>Weld strength (N/mm)</td>
<td>39.23</td>
<td>46.68</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Weld width (mm)</td>
<td>2.36</td>
<td>2.16</td>
<td></td>
<td></td>
</tr>
<tr>
<td>MSNR</td>
<td>0.2130</td>
<td>0.3208</td>
<td>0.3172</td>
<td></td>
</tr>
</tbody>
</table>

Improvement of the MSNR = 0.1042 dB

CONCLUSIONS

Optimization of multiple quality characteristics of laser transmission welded acrylic parts using Taguchi quality loss function is studied in this paper. The application of this optimization technique converts the multiple performance characteristics to a single performance characteristic called multiple signal-to-noise ratio and, therefore, simplifies the optimization procedure. Following conclusions can be drawn on the basis of the results obtained:

(1) The optimal level of the laser transmission welding parameters for maximum weld strength and minimum weld width are: laser power at 24 W, welding speed at 380 mm/min and focal distance at +8 mm.

(2) At the optimal level of parameters setting, the attainable maximum weld strength is 46.68 N/mm with a minimum weld width of 2.16 mm, against the initial values of 39.23 N/mm and 2.36 mm, respectively.

(3) Laser power, welding speed and focal distance significantly affect the laser transmission welding quality in the operating range of process parameters.

(4) The multiple S/N ratio for the optimal setting is improved by 0.1042 dB, which shows that there is considerable improvement in multiple performance characteristics with this optimization technique in the laser transmission welding operation.

REFERENCES


Effects of process parameters on weld bead geometry and metal transfer in synergic MIG welding of 304L Stainless Steel

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M.S.Niranjan
Delhi, Delhi, India

ABSTRACT
The objective of this paper is to analyze voltage transients associated with Synergic MIG welding of 304L SS for various plate thicknesses (3 mm, 6 mm, 8 mm & 12 mm) with 100% Argon as the shielding gas and the relation between these transients and observation is used to analyze the mode of metal transfer and weld bead geometry. The most popular method to identify the mode of metal transfer and the moment in which the transfer occurs is based on an oscillographical analysis of voltage (voltage versus time) by using DSO with Synergic MIG machine. With the use of oscilloscope, it is possible to observe the format of the voltage traces produced by welding processes. For instance, during short-circuiting transfer, when the droplet is starting its development, voltage oscillates around a mean value but tends to zero when the drop touches the pool (short-circuit). The weld bead geometry plays an important role in determining the mechanical properties of a weld joint. Its geometric parameters such as bead width, reinforcement height, and depth of penetration depends on the process parameters, such as wire feed rate, welding current, welding speed, plate thickness, etc. Therefore, it is important to set up proper welding parameters to produce a good weld bead.

Keywords: Synergic MIG; Metal transfer; Bead geometry and shape relationships; Digital signals Oscilloscope; Voltage transients.

INTRODUCTION
A review of the available literature reveals many studies relating to the fundamental physical and chemical properties of 304L stainless steel (SS) [1-4]. Due to its excellent mechanical and anticorrosion properties, and the ease with which it may be formed and welded, 304L SS is widely used throughout the chemical and automotive industries, nuclear power plants, aircrafts, conveyers, bridges, health care facilities etc. The joining of these parts is often achieved by welding, and consequently, the favourable welding characteristics of 304L SS play an important role in its selection as the material of choice. It is recognized that 304L SS can be successfully welded using a variety of techniques, including Gas Metal Arc Welding (GMAW), Shielded Metal Arc Welding (SMAW), Gas Tungsten Arc Welding (GTAW), Submerged Arc Welding (SAW), Flux Cored Arc Welding (FCAW) and Plasma Arc Welding (PAW) [5-6]. With the increase of automation in arc welding, the selection of welding procedure must be more specific to ensure that adequate bead quality is obtained [7-8]. Several researchers have attempted to investigate the effects of various process variables on the weld bead geometry and metal transfer.

Also the mechanical strength of welds is influenced by the compositions of the metal and to greater extent by the weld bead geometry and shape relationships and in turn the weld bead geometry is influenced by the direct and indirect welding parameters. The study of weld bead geometry and shape relationships is important as these dimensions and ratios decide to a great extent, the load bearing capacity of weldments. This includes the study of penetration (p), bead width (w), height of reinforcement/crown height (h), ratio of bead width to penetration (w/p) also known as weld penetration shape factor (WPSF), ratio of bead width to reinforcement height (w/h) also known as weld reinforcement form factor (WRFF), and % dilution (%D) which is percentage ratio of area of base metal melted to total area of the weld bead at a given cross-section of the weld deposit [9]. All these terms are depicted in Figure 1.
Metal transfer describes the process of the molten metal movement from the electrode tip to the work piece in metal inert gas welding. A better understanding of the metal transfer process is important for improvements in the quality and productivity of welding [10]. The mode of metal transfer significantly influences the chemical composition and the properties of weld metal, metallurgy of weld metal, weld pool stability, fumes levels, arc stability, spatter losses and weld bead geometry/strength of weldment. The modes of metal transfer is affected mainly by the type of the arc, welding current, electrode polarity, arc voltage, nozzle to plate distance, gas composition and flow rate [11]. Depending upon the welding conditions, there are different ways in which this transfer of metal takes place. One of the most up-to-date and comprehensive reviews of metal transfer modes during arc welding was written by Lancaster [12]. According to the International Institute of Welding (IIW) nomenclature referenced in his book [13], metal transfer can be classified into three main groups: free-flight transfer, bridging transfer and slag protected transfer. Free-flight mode of metal transfer can still be sub-classified as drop, repelled, projected, streaming or rotating. In free-flight transfer, the electrode does not contact the molten metal pool. Metal droplet detaches from the tip of the electrode and move across the arc column. When the electrode contacts the weld pool, bridging transfer occurs. For welding process that uses large amount of fluxes, metal transfer may involve layers of slag, known as slag-protected transfer. The mode of metal transfer, which are operative at any instances during welding, are also dependent upon several forces that act upon the molten droplet growing at the tip of the electrode [14-15].

Many attempts have been made by various researchers with an objective of identifying and explaining the mechanism that governs the phenomenon of metal transfer [16-17]. The physics of metal transfer is not yet well understand, due to the facts that the arcs are too small, the temperatures are too high, and the metal transfer is at too high rate. Thus many mechanisms affecting metal transfer have been suggested [18]. Besides the metal transfer mode identification, it is important, in many cases, to know the relationship between the metal under transfer and the signals of welding current, arc voltage, arc sound, etc. The knowledge of this interaction becomes important when the goal is the optimization and control of processes, such as pulsed and short circuiting GMAW. The most popular method to identify the type of metal transfer and the moment in which the transfer occurs is based on an oscillographical analysis of voltage (voltage versus time) [19].

**EXPERIMENTAL SET-UP**

All the weldments in this study were carried out using microprocessor based Synergic MIG welding machine “EWM force Arc 521” with the semi-mechanized welding station used during the experimentation. The power source control takes place on the one-knob principle (Synergic). The electrical power and the wire feed speed are adjusted steplessly with one knob along a programmed characteristic. The arc length can also be corrected. The system comprising inverter and control gives the arc the capacity to react very fast to various influences in order to keep the power parameters constant independently of the cable lengths in the welding power circuit [20]. This welding machine is connected with digital signal oscilloscope in parallel and DSO is connected with printer in order get the printout of the transient simultaneously during welding process. Welding gun was placed perpendicularly to stainless steel 304L plates. In the welding experiments, a 308L filler wire with a 1.2 mm diameter was used. All experiments were carried out with contact tip to work distance (CTWD) of 20 mm, using pure argon as shielding gas at a three different flow rate i.e. 10 lit/min (5.886×10⁻³ ft³/sec), 13 lit/min (7.6518×10⁻³ ft³/sec) & 15 lit/min (8.829×10⁻³ ft³/sec). A direct current power source was used to perform the bead on plate welds by means of Synergic MIG process. ‘Bead on plate’ technique was employed for depositing the weld beads on stainless steel plate of thickness 3, 6, 8 & 12 mm using semi mechanised welding station. The experimental set up is shown in Figure 2. The weld bead samples are cut from each weld bead at 15 mm intervals, with the first sample...
being located at 15 mm behind the trailing edge of the crater end to eliminate the end effects.

The transverse faces of the specimens were further prepared for study of weld bead geometry. Specimens were polished with various grades of emery papers, starting with 150, 180, 320, 400, 800, 1200 and SIA Sianor B 1600. After this the specimens were polished on rotating disc with a paste of alumina abrasive powder. Water was used as coolant. Finally the specimens were etched with mixture of three parts of HCl and one part of HNO₃. All the test specimens were washed off with water to reveal the geometry of the weld bead. Several critical parameters, such as bead penetration, bead width & bead heights were measured by projecting it on the profilometer.

Figure 2. Experimental set up for synergic MIG welding

RESULTS

Table 1. V transient, weld bead profile, cross section view and optimum values for spray transfer on 3 mm plate thickness.

<table>
<thead>
<tr>
<th>P. T</th>
<th>I</th>
<th>G</th>
<th>V</th>
<th>S</th>
<th>Weld bead</th>
</tr>
</thead>
<tbody>
<tr>
<td>3</td>
<td>159</td>
<td>15</td>
<td>22.2</td>
<td>32.4</td>
<td></td>
</tr>
<tr>
<td>P</td>
<td>B.W.</td>
<td>R.H.</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1.49</td>
<td>7.09</td>
<td>3.36</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 2. V transient, weld bead profile, cross section view and optimum values for spray transfer on 6 mm plate thickness.

<table>
<thead>
<tr>
<th>P. T</th>
<th>I</th>
<th>G</th>
<th>V</th>
<th>S</th>
<th>Weld bead</th>
</tr>
</thead>
<tbody>
<tr>
<td>6</td>
<td>235</td>
<td>15</td>
<td>27.6</td>
<td>32.4</td>
<td></td>
</tr>
<tr>
<td>P</td>
<td>B.W.</td>
<td>R.H.</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>2.94</td>
<td>11.26</td>
<td>3.27</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 3. V transient, weld bead profile, cross section view and optimum values for spray transfer on 8 mm plate thickness.

<table>
<thead>
<tr>
<th>P. T</th>
<th>I</th>
<th>G</th>
<th>V</th>
<th>S</th>
<th>Weld bead</th>
</tr>
</thead>
<tbody>
<tr>
<td>8</td>
<td>263</td>
<td>15</td>
<td>31</td>
<td>32.4</td>
<td></td>
</tr>
<tr>
<td>P</td>
<td>B.W.</td>
<td>R.H.</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.39</td>
<td>13.05</td>
<td>3.6</td>
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<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 4. V transient, weld bead profile, cross section view and optimum values for spray transfer on 12 mm plate thickness.

<table>
<thead>
<tr>
<th>P. T</th>
<th>I</th>
<th>G</th>
<th>V</th>
<th>S</th>
<th>Weld bead</th>
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<tr>
<td>12</td>
<td>319</td>
<td>15</td>
<td>33.9</td>
<td>32.4</td>
<td></td>
</tr>
<tr>
<td>P</td>
<td>B.W.</td>
<td>R.H.</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>3.49</td>
<td>15.54</td>
<td>3.59</td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 5. Bead dimensions for different G, S and I on 3 mm plate thickness

| P. T | W C A V W S GFR P B.W. R.H. (mm) (Amp) (volts) (cm/min) (l/min) (mm) (mm) |
|------|---------------------------------|---------------------------------|---------------------------------|---------------------------------|---------------------------------|---------------------------------|---------------------------------|
| 3    | 159 | 22.2 | 47.4 | 10 | 1.54 | 6.33 | 2.57 |
| 13   | 2.05 | 7.39 | 2.85 |
| 15   | 0.91 | 7.44 | 3.48 |
| 32.4 | 1.49 | 7.09 | 3.36 |
| 41.4 | 1.75 | 5.21 | 3.12 |
| 47.4 | 1.11 | 4.95 | 3.03 |
Table 6. Bead dimensions for different G, S and I on 6 mm plate thickness

<table>
<thead>
<tr>
<th>P.T.</th>
<th>W</th>
<th>C</th>
<th>V</th>
<th>W</th>
<th>S</th>
<th>GFR</th>
<th>P</th>
<th>B.W.</th>
<th>R.H.</th>
</tr>
</thead>
<tbody>
<tr>
<td>(mm)</td>
<td>(Amp)</td>
<td>(volts)</td>
<td>(cm/min)</td>
<td>(l/min)</td>
<td>(mm)</td>
<td>(mm)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>6</td>
<td>235</td>
<td>27.6</td>
<td>32.4</td>
<td>10</td>
<td>1.02</td>
<td>8.12</td>
<td>3.07</td>
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<td>0.84</td>
<td>6.64</td>
<td>2.22</td>
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<td>144</td>
<td>1.99</td>
<td>7.25</td>
<td>3.00</td>
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<td></td>
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</table>

Table 7. Bead dimensions for different G, S and I on 8 mm plate thickness

<table>
<thead>
<tr>
<th>P.T.</th>
<th>W</th>
<th>C</th>
<th>V</th>
<th>W</th>
<th>S</th>
<th>GFR</th>
<th>P</th>
<th>B.W.</th>
<th>R.H.</th>
</tr>
</thead>
<tbody>
<tr>
<td>(mm)</td>
<td>(Amp)</td>
<td>(volts)</td>
<td>(cm/min)</td>
<td>(l/min)</td>
<td>(mm)</td>
<td>(mm)</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>8</td>
<td>241</td>
<td>31</td>
<td>32.4</td>
<td>10</td>
<td>2.14</td>
<td>11.07</td>
<td>2.43</td>
<td></td>
<td></td>
</tr>
<tr>
<td>263</td>
<td>32.4</td>
<td>15</td>
<td>2.14</td>
<td>13.05</td>
<td>3.32</td>
<td></td>
<td></td>
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<td></td>
</tr>
<tr>
<td>241</td>
<td>32.4</td>
<td>15</td>
<td>1.94</td>
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<td>12.51</td>
<td>3.15</td>
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<td></td>
</tr>
<tr>
<td>263</td>
<td>3.39</td>
<td>13.05</td>
<td>3.60</td>
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<td></td>
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<td></td>
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<td></td>
</tr>
</tbody>
</table>

Table 8. Bead dimensions for different G, S and I on 12 mm plate thickness

<table>
<thead>
<tr>
<th>P.T.</th>
<th>W</th>
<th>C</th>
<th>V</th>
<th>W</th>
<th>S</th>
<th>GFR</th>
<th>P</th>
<th>B.W.</th>
<th>R.H.</th>
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<tbody>
<tr>
<td>(mm)</td>
<td>(Amp)</td>
<td>(volts)</td>
<td>(cm/min)</td>
<td>(l/min)</td>
<td>(mm)</td>
<td>(mm)</td>
<td></td>
<td></td>
<td></td>
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<tr>
<td>12</td>
<td>319</td>
<td>33.9</td>
<td>32.4</td>
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<td>2.87</td>
<td>14.71</td>
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<td>319</td>
<td>32.4</td>
<td>15</td>
<td>3.49</td>
<td>15.54</td>
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<tr>
<td>275</td>
<td>32.4</td>
<td>15</td>
<td>3.09</td>
<td>12.63</td>
<td>2.94</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>291</td>
<td>3.12</td>
<td>13.11</td>
<td>4.33</td>
<td></td>
<td></td>
<td></td>
<td></td>
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<td></td>
</tr>
<tr>
<td>300</td>
<td>3.45</td>
<td>14.28</td>
<td>5.04</td>
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<td></td>
</tr>
</tbody>
</table>

ANALYSIS OF RESULTS

Effect of process parameters on metal transfer

With the use of oscilloscope, it is possible to observe the format of the current and voltage traces produced by welding processes. These signal waves may mimic some metal transfer modes. For instance, during short circuiting transfer, when the droplet is starting its development, voltage oscillates around a mean value but tends to zero when the drop touches the pool (short-circuit). A voltage peak happens just after the drop detachment, due to the arc reignition phenomenon. Thus, it is possible to detect droplet detachment electrically by using a storage oscilloscope or transient recorder. The detachment generates a small blip on the arc voltage trace, probably due to the increased electrical resistance of the droplet neck just before separation.

However, the spray transfer for 3 mm plate was observed at current 159 A, gas flow rate 15 l/min (8.828×10⁻³ ft³/sec), voltage 22.2 volts and welding speed of 32.4 cm/min (0.0177 ft/sec) using 100% argon shielding gas as shown in Table 1. For 6 mm plate thickness the spray transfer was observed at current 235 A, gas flow rate 15 l/min (8.829×10⁻³ ft³/sec), voltage 27.6 volt and welding speed of 32.4 cm/min (0.0177 ft/sec) as shown in Table 2. For 8 mm plate thickness the spray transfer was observed at current 263 A, gas flow rate 15 l/min (8.829×10⁻³ ft³/sec), voltage 31 volts and welding speed of 32.4 cm/min (0.0177 ft/sec) as shown in Table 3. Similarly for 12 mm plate thickness the spray transfer was observed at welding current of 319 A, gas flow rate 15 l/min (8.829×10⁻³ ft³/sec), arc voltage 33.9 volts and welding speed of 32.4 cm/min (0.0177 ft/sec) as shown in Table 4. These results show that it is the optimum value for spray transfer in Synergic MIG welding.

Good quality welds could be achieved only with axial spray type of transfer. High welding current and high voltage produced the mixed mode but predominantly axial spray and occasional short circuiting transfer in between. The weld bead ripples were fairly uniform and the general appearances of the bead were very good.

Effects of process parameters on penetration

For 3 mm plate thickness, the welding speed and gas flow rate was fixed as 47.4 cm/min (0.0259 ft/sec) and 15 l/min (8.829×10⁻³ ft³/sec) respectively and the change in depth of penetration was drawn with increasing welding current for 22.2 V values as shown in Fig. 3. The biggest penetration value was obtained as 1.99 mm in 144 A and 22.2 V condition, while the smallest one as 0.40 mm in 130 A and 22.2 V. In all three conditions, the depth of penetration increases with increasing welding current. The same trends were shown...
for 6 mm, 8 mm & 12 mm plate thickness as shown in Fig. 6, Fig. 9 & Fig. 10 respectively.

The graphs of welding speed vs. penetration were drawn using Table 5, Table 6, Table 7 and Table 8, in 22.2 V, 27.6 V, 31 V & 33.9 V constant arc voltages for 159 A, 235 A, 263 A & 319 A values for 3 mm, 6 mm, 8 mm and 12 mm plate thickness respectively as shown in Fig. 4. The depth of penetration increases with increasing welding speed up to 41.4 cm/min (0.0226 ft/sec) point which was the optimum value to obtain maximum penetration, because it begins to decreasing after this point again linearly.

The graph of gas flow rate vs. penetration was drawn using Table 5, Table 6, Table 7 and Table 8, with 22.2 V, 27.6 V, 31 V and 33.9 V constant arc voltage for 159 A, 235 A, 263 A and 319 A and gas flow rate of 10 lit/min (5.886×10⁻³ ft³/sec), 13 lit/min (7.6518×10⁻³ ft³/sec) and 15 lit/min (8.829×10⁻³ ft³/sec) values for 3 mm, 6 mm, 8 mm and 12 mm plate thickness respectively as shown in Fig. 5. The depth of penetration increases with increasing gas flow rate up to 13 lit/min (7.6518×10⁻³ ft³/sec) point which was the optimum value to obtain maximum penetration, because it begins to decreasing after this point again linearly.

Effects of process parameters on weld bead width

Weld bead width increased from 6.33 mm to 7.44 mm, 8.12 mm to 11.67 mm, 11.07 mm to 13.09 mm and 14.71 mm to 16.49 mm, for 3 mm, 6 mm, 8 mm and 12 mm plate thickness respectively when gas flow rate was increased from 10 lit/min (5.886×10⁻³ ft³/sec) to 15 lit/min (8.829×10⁻³ ft³/sec) as shown in Fig. 8. Weld bead width increased from 6.19 mm to 7.25 mm, 10.25 mm to 11.26 mm, 11.28 mm to 13.05 mm and 12.63 mm to 14.28 mm for 3 mm, 6 mm, 8 mm and 12 mm plate thickness respectively when current was increased from low level to high level as shown in Fig. 3, Fig. 6, Fig. 9 & Fig. 10.

Weld bead width decreased from 7.09 mm to 4.95 mm, 10.59 mm to 7.22 mm, 13.05 mm to 11.91 mm and 15.54 mm to 13.28 mm for 3 mm, 6 mm, 8 mm and
12 mm plate thickness respectively when welding speed was increased from 32.4 cm/min (0.0177 ft/sec) to 47.4 cm/min (0.0259 ft/sec) as shown in Fig. 7.

![Figure 6. Penetration, Reinforcement height & Bead width vs. welding current for 6 mm plate thickness](image)

Figure 6. Penetration, Reinforcement height & Bead width vs. welding current for 6 mm plate thickness

![Figure 7. Bead width vs. welding speed for 3, 6, 8 & 12 mm plate thickness](image)

Figure 7. Bead width vs. welding speed for 3, 6, 8 & 12 mm plate thickness

**Effects of process parameters on reinforcement height**

Reinforcement height increases from 2.57 mm to 3.48 mm, 3.07 mm to 3.79 mm, 2.43 mm to 2.65 mm and 3.01 mm to 4.09 mm for 3 mm, 6 mm, 8 mm and 12 mm plate thickness respectively when gas flow rate was increased from 10 lit/min (5.886×10^{-3} ft³/sec) to 15 lit/min (8.829×10^{-3} ft³/sec) as shown in Fig. 12.

The reinforcement height increased from 1.91 mm to 3.00 mm, 3.09 mm to 3.27 mm, 3.14 mm to 3.60 mm and 2.94 mm to 5.04 mm for 3 mm, 6 mm, 8 mm and 12 mm plate thickness respectively when current was increased from low level to high level as shown in Fig. 3, Fig. 6, and Fig. 9 & Fig. 10. This was due to the larger amount of metal deposited per unit length. The increased proportion of weld metal deposited with the increase in welding wire feed rate got distributed in the bead width and the height of reinforcement.

The reinforcement height decreased from 3.36 mm to 3.03 mm, 3.99 mm to 2.99 mm, 3.32 mm to 2.80 mm and 3.59 mm to 3.01 mm for 3 mm, 6 mm, 8 mm and 12 mm plate thickness respectively when welding speed was increased from 10 lit/min (5.886×10^{-3} ft³/sec) to 15 lit/min (8.829×10^{-3} ft³/sec) as shown in Fig. 11. This could be due to the fact that weld pool size is affected by cooling rate, which can decrease by increasing the current or by decreasing the travel speed. Details are shown in Table 5, Table 6, Table 7 and Table 8.
CONCLUSIONS

Following conclusion was drawn from this analysis:

1. The spray transfer for 3 mm plate were observed at current 159 A, gas flow rate 15 lit/min \((8.829 \times 10^{-3} \text{ ft}^3/\text{sec})\), voltage 22.2 volts and welding speed of 32.4 cm/min \((0.0177 \text{ ft/sec})\).

For 6 mm plate thickness the spray transfer were observed at current 235 A, gas flow rate 15 lit/min \((8.829 \times 10^{-3} \text{ ft}^3/\text{sec})\), voltage 27.6 volt and welding speed of 32.4 cm/min \((0.0177 \text{ ft/sec})\).

For 8 mm plate thickness the spray transfer were observed at current 263 A, gas flow rate 15 lit/min \((8.829 \times 10^{-3} \text{ ft}^3/\text{sec})\), voltage 31 volts and welding speed of 32.4 cm/min \((0.0177 \text{ ft/sec})\).

For 12 mm plate thickness the spray transfer were observed at welding current of 319 A, gas flow rate 15 lit/min \((8.829 \times 10^{-3} \text{ ft}^3/\text{sec})\), arc voltage 33.9 volts and welding speed of 32.4 cm/min \((0.0177 \text{ ft/sec})\).

2. The depth of penetration increased with increasing welding current for all plate thickness.

The depth of penetration increases with increasing welding speed up to 41.4 cm/min \((0.0226 \text{ ft/sec})\) point which was the optimum value to obtain maximum penetration, because it begins to decrease after this point again linearly.

The depth of penetration increases with increasing gas flow rate up to 13 lit/min \((7.6518 \times 10^{-3} \text{ ft}^3/\text{sec})\) point which was the optimum value to obtain maximum penetration, because it begins to decrease after this point again linearly.
3. Weld bead width increases when gas flow rate increases from 10 lit/min \((5.886\times10^{-3} \text{ ft}^3/\text{sec})\) to 15 lit/min \((8.829\times10^{-3} \text{ ft}^3/\text{sec})\).

   Bead width increases when current is increasing from low level to high level.

   But, weld bead width decreases when welding speed increases from 32.4 cm/min \((0.0177 \text{ ft/sec})\) to 47.4 cm/min \((0.0259 \text{ ft/sec})\).

4. Reinforcement height increases when gas flow rate increases from 10 lit/min \((5.886\times10^{-3} \text{ ft}^3/\text{sec})\) to 15 lit/min \((8.829\times10^{-3} \text{ ft}^3/\text{sec})\).

   Reinforcement height also increases when current was increased from low level to high level.

   Reinforcement height decreased when welding speed is increases from 10 lit/min \((5.886\times10^{-3} \text{ ft}^3/\text{sec})\) to 15 lit/min \((8.829\times10^{-3} \text{ ft}^3/\text{sec})\).

REFERENCES


